

Spray Charging and Trapping Scrubber for
Fugitive Particle Emission Control

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SPRAY CHARGING AND TRAPPING SCRUBBER FOR
FUGITIVE PARTICLE EMISSION CONTROL

BY

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Leakage	Aerosols	Fugitive Emissions	14G 07D
Scrubbers	Air Curtains	Spray Charging and	07A, 13I 13M
Charging	Jets	Trapping Scrubber	20D
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ABSTRACT

The control of fugitive particle emissions (FPE) with a Spray Charging and Trapping (SCAT) scrubber was evaluated both theoretically and experimentally. The SCAT uses air curtain and/or jets to contain, convey, and divert the FPE into a charged spray scrubber.

Experiments were performed on a 225 m³/min bench-scale spray scrubber to verify the theory and feasibility of collecting fugitive particles with charged water spray. The effects of charge levels on drops and particles, nozzle type, drop size, gas velocity, and liquid/gas ratio on collection efficiency was determined experimentally. The results of the experiments and the comparison between theory and data are presented.

An air curtain was developed for conveying the FPE to the spray scrubber. The design and air flow field for the air curtain are presented.

A prototype SCAT was built to study the effect of crosswind and hot buoyant plume. Available data revealed that the air curtain was successful in deflecting crosswind up to 15 mph and containing a hot buoyant plume. Theories were developed for predicting the trajectories of the air curtain jet stream and the hot buoyant plume.

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LIST OF ABBREVIATIONS

- SCAT - Spray Charging and Trapping
- FPE - Fugitive particle emission

- ND/UP - Neutral drops/uncharged particles
- CD/UP - Charged drops/uncharged particles
- ND/CP - Neutral drops/charged particles
- CD/CP - Charged drops/charged particles

NOMENCLATURE

- A = cross-sectional area, cm^2
- A_s = corss-sectional area of side outlet, cm^2
- a = experimental constant, dimensionless
- b = width of manifold, cm
- b = half width of the plane jet, cm
- b = width of mixing region, cm
- b = plume radius, cm
- b_0 = plume radius at exit, cm
- b^* = dimensionless plume radius, dimensionless
- C_d = slot discharge coefficient, fraction
- C^1 = Cunningham slip correction factor, dimensionless
- C_1 = static pressure parameter
- C_2 = portion of pressure head recovery along the duct and loss at the side outlet, fraction
- C_3 = empirical constant, dimensionless
- C_4 = empirical constant, dimensionless
- C_5 = empirical constant, dimensionless
- c_p = specific heat, $\text{cal/g-}^\circ\text{C}$
- c_{pi} = inlet particle concentration, mg/cm^3
- c_{po} = outlet particle concentration, mg/cm^3
- d_d = drop diameter, cm
- d_h = hydraulic diameter of duct, cm
- d_p = particle diameter, cm or μm
- d_{pa} = aerodynamic particle diameter, μm
- E = collection efficiency, fraction

NOMENCLATURE (cont.)

- E = electric field strength, v/cm
- E_0 = uniform external electric field strength, V/cm
- e = 4.8×10^{-10} esu = 1.6×10^{-19} C
- F = buoyancy flux, cm^4/s^3
- F_e = electrostatic force, g-cm/s^2
- F_0 = buoyancy flux at source, cm^4/s^3
- f = friction factor, dimensionless
- f_A = drop coverage, fraction
- f_e = spatial dependence of the electrostatic force in terms of dimensionless coordinates, dimensionless
- g = constant of gravity, cm/s
- g_c = conversion constant
- H_m = manifold pressure head loss, cm W.C.
- H_s = pressure head loss in the side outlet, cm W.C.
- h = air curtain range, cm
- h_m = maximum air curtain range, cm
- J = momentum per unit length of slot, g/s^2
- K_c = Coulombic force parameter, dimensionless
- K_{ex} = external electric field force parameter, dimensionless
- K_{ic} = charged particle image force parameter, dimensionless
- K_{icp} = electric dipole interaction force parameter, dimensionless
- K_{ip} = charged collector image force parameter, dimensionless
- K_p = inertial impaction parameter, dimensionless
- k = Boltzmann's constant, $\text{J/}^\circ\text{K}$
- k = nozzle discharge coefficient, fraction

NOMENCLATURE (cont.)

- k = experimental constant, dimensionless
- L = air curtain slot length, cm
- L = air curtain/spray scrubber separation, cm
- L_B = buoyancy length, cm
- ℓ = Prandtl mixing length, cm
- ℓ = distance between air curtain and hot source, cm
- M = momentum flux, cm^3/s^3
- M_O = momentum flux at source, cm^3/s^3
- m_L = mass flow rate of drops, kg/s
- N = ion density, /m
- N_d = drop injection rate, /s
- N_p = total number of elementary charges on particles, number
- N_{pe} = drop Reynolds number, dimensionless
- n_c = number of charges, number
- n_p = inlet particle number concentration, / cm^3
- Pt_d = particle penetration for particle diameter " d_{pa} ", fraction
- $\overline{Pt}$ = overall penetration, fraction
- p = static pressure, Pa
- p_L = static pressure of liquid, Pa
- p_o = initial static pressure, Pa
- p^* = dimensionless static pressure, dimensionless
- Q_d = charge on drop, C
- Q_G = volumetric gas flow rate, cm^3/s
- Q_G^* = dimensionless volumetric gas flow rate, cm^3/s

NOMENCLATURE (cont.)

Q_{Gj}	= volumetric flow rate at jet exit, cm^3/s
Q_{Gs}	= volumetric flow rate per unit slot length, $\text{cm}^3/\text{cm-s}$
Q_L	= volumetric liquid flow rate, cm^3/s
Q_p	= charge on particle, C
q	= entrainment ratio, dimensionless
R	= pressure regain coefficient, dimensionless
R	= duct radius, cm
$\bar{R}$	= average pressure regain coefficient, dimensionless
R_d	= drop length, cm
R_1	= final pressure regain coefficient, dimensionless
R_0	= initial pressure regain coefficient, dimensionless
r	= cylindrical coordinate, cm
T	= temperature, $^{\circ}\text{K}$
T_a	= ambient air temperature, $^{\circ}\text{C}$
T_s	= plume temperature, $^{\circ}\text{C}$
t	= time, s
t^*	= dimensionless time, dimensionless
u_a	= ambient air velocity, cm/s
u_d	= drop velocity, cm/s
u_e	= entrainment velocity, cm/s
u_G	= gas velocity, cm/s
u_{GD}	= initial gas velocity in the manifold, cm/s
u_G^*	= dimensionless gas velocity, dimensionless
u_{Gb}	= velocity at the edges of the mixing zone, cm/s

NOMENCLATURE (cont.)

- u_{Gc} = centerline velocity, cm/s
- u_{Gco} = inertial centerline velocity, cm/s
- u_{Gc}^* = dimensionless centerline velocity, dimensionless
- u_{Gj} = slot exit velocity, cm/s
- u_{Gs} = average gas velocity in side outlet, cm/s
- u_{Gw} = average gas velocity near the wall, cm/s
- u_p = particle velocity, cm/s
- u_p^* = dimensionless particle velocity, dimensionless
- u_o = undisturbed upstream velocity, cm/s
- V_s = gas volume swept clean per cm^3 of drop, cm^3/cm^3
- v_s = vertical component of gas velocity, cm/s
- w = slot width, cm
- x = coordinate, cm
- x^1 = coordinate, cm
- x^+ = dimensionless distance, dimensionless
- y = coordinate, cm
- y^1 = coordinate, cm
- y_1 = distance of the limiting trajectory from the drop surface at 90° from the stagnation point, cm
- y_o = limiting trajectory, cm
- z = height of plume rise, cm
- z^* = dimensionless height of plume rise, dimensionless

Greek

- α = entrainment constant, dimensionless
- α = spray angle, degree

NOMENCLATURE (cont.)

Greek (cont.)

- α_0 = incident angle, degree
- β = scrubber rotation angle, degree
- γ = buoyancy spreading factor, dimensionless
- ΔN = particle removal rate, /s
- Δp_G = pressure drop, cm W.C.
- δ = boundary layer thickness, cm
- ϵ = eddy viscosity, cm^2/s
- ϵ_d = dielectric constant of drop, F/cm
- ϵ_0 = dielectric constant of the gas, F/cm
- ϵ_p = dielectric constant of the particle, F/cm
- η = single collector collection efficiency, fraction
- $\bar{\eta}$ = average single collector collection efficiency, fraction
- η_p = potential flow single drop collection efficiency, fraction
- η_v = viscous flow single drop collection efficiency, fraction
- θ = jet expansion angle, degree
- θ = discharge angle, degree
- λ_b = loss coefficient for the diverted flow, dimensionless
- λ_m = loss coefficient for the manifold, dimensionless
- λ_s = loss coefficient for the side outlet, dimensionless
- μ_s = gas viscosity, g/cm-s
- ν_G = kinematic viscosity of gas, cm^2/s
- ρ_a = ambient air density, g/cm^3
- ρ_G = gas density, g/cm^3
- ρ_{GC} = gas density at the centerline, g/cm^3

NOMENCLATURE (cont.)

Greek (cont.)

ρ_{Gj} = gas density at jet exit, g/cm³

σ = attachment coefficient, fraction

σ = experimental constant = 7.67

τ = turbulent shear stress, g/cm²

τ_L = laminar shear stress, g/cm²

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SECTION 1

SUMMARY AND CONCLUSIONS

SUMMARY

Controlled disposal of fugitive particles is the permanent way to control fugitive particle emissions, because it precludes the redispersal of particles. This technique involves gathering and conveying the fugitive emissions to conventional control devices. Present methods used to contain and gather fugitive emissions, such as total building enclosure and evacuation, or secondary hooding at the local source of emissions, are ineffective and costly.

The SCAT (Spray, Charging, and Trapping) system is a novel, controlled disposal scrubber system for controlling industrial process fugitive emissions. It uses air curtain(s) and/or air push jet(s) to contain, divert, and convey fugitive particles into a particle scrubber or the receiving hood of a particle scrubber. The SCAT system uses, but is not limited to, charged water sprays for removing particles entrained in the gas stream.

The SCAT system has three basic functions: (1) contain and convey the fugitive emissions to the scrubber, (2) remove the particles with charged water sprays, and (3) collect the particles and water drops. There is insufficient published information on the design and performance of an air curtain and a charged water spray scrubber. To generate design data, the air curtain and the charged spray scrubber were studied individually in separate bench scale experiments. A prototype SCAT system was built based on bench scale data. The prototype SCAT was then used in studying the effects of hot buoyant plumes and crosswinds on the SCAT system performance.

PRELIMINARY EXPERIMENTS

Air Curtain Study

An air curtain is a sheet of air blown out of circular or rectangular nozzles at high speed. The design of the SCAT requires information on certain parameters of the jet stream. These are the jet expansion angle, the air entrainment ratio, and the effects of hot buoyant plume and crosswind conditions. The jet expansion angle determines the overall cross-sectional dimensions of the receiving hood. The air entrainment ratio determines the volumetric flowrate of the gas to be cleaned. The crosswind and heat effect dictate the placement of air curtains and the receiving hood. All these parameters were studied experimentally in the present research.

...

Air Curtain Manifold

The air curtain manifold should give a uniform air jet along its length and the discharge direction should be nearly perpendicular to the flow direction in the manifold. Several air curtain distribution manifolds were designed, built and tested. A tapered rectangular cross section gave the best results. An adjustable plate whose curvature could be changed was placed in the duct to decrease the flow area along the duct. The air jet nozzle was a continuous slot formed by two parallel plates which protruded from one side of the duct. The distance between the plates, which is the slot width, could be adjusted.

The slot was divided by thin deflector vanes 5.1 cm (2 inches) apart. Since the air curtain manifold was intended for testing at a variety of operating conditions, the use of thin flat plates eliminated the need to change the turning vanes for each operating conditions as would have been necessary if curved turning vanes were used.

The discharge distribution for this manifold with linear decreasing flow area was found to be uniform and the discharge angle close to 90°.

Jet Expansion Angle and Air Entrainment Ratio

The jet expansion angle and air entrainment ratio were determined for the air curtain jet stream. They were calculated from velocity distribution measurements at several downstream locations from the manifold. The calculated jet expansion angle varied between 20° and 28° which is smaller than the 32° to 34° reported by Tuve and Priester (1944) and the 30° to 40° suggested by Hemeon (1963). The average of the measured jet expansion angles agrees with that calculated by Abramovich (1963) for pure momentum jets.

The measured jet stream centerline axial velocity decay agrees with that predicted from Prandtl's eddy viscosity model for turbulent flow. The measured air entrainment ratio is not as well defined by mathematical models presently available.

Charged Spray Scrubber Study

The charged spray scrubber has been studied theoretically and experimentally by a number of researchers. However, data reported by these researchers are not sufficient for verifying the mathematical model for predicting efficiency.

In order to obtain design data under well defined conditions, a charged spray scrubber system was built.

The particle charger section consisted of two rows of corona wires with tubular ground electrodes.

The spray banks were removable and their configuration varied depending on nozzle types. Water was supplied to the spray nozzles by an electrically isolated plastic pump.

The drops were charged by induction and the spray nozzles could either be kept at ground potential or maintained at high voltage. Based on results from an auxiliary experiment, the first method was used. A high voltage grid as-

sembly was placed in front of the nozzles to charge the water drops. This arrangement simplified the electrical isolation problems. The distance between the grid and nozzle was adjustable for maximizing the drop charge level.

The charging grid and water supply were isolated from the scrubber shell. They were kept dry with purging air at the point where they passed through the shell.

Two types of nozzles were tested. The hook type nozzles produced drops with mean diameter around 240 μm at a nozzle pressure of 430 kPa. The discharge coefficient and spray angle for this nozzle were 0.63 and 100°, respectively. The pigtail type nozzle produced 350 μm drops. The discharge coefficient and spray angle were 0.7 and 87°, respectively.

All experiments were performed under ambient conditions. The particle collection efficiency of the spray scrubber was measured for the following four conditions:

1. ND/UP (Neutral drops/uncharged particles),
2. CD/UP (Charged drops/uncharged particles),
3. ND/CP (Neutral drops/charged particles), and
4. CD/CP (Charged drops/charged particles).

The particle collection efficiency of the scrubber was found to improve by charging either the water drops or the particles. Measured drop charge and particle charge levels were $+5.8 \times 10^{-7}$ C/g and -1.5×10^{-5} C/g, respectively. Test particles were hydrated lime with mass median diameter of 3.5 μm and geometric standard deviation around 2. Further improvement was obtained when the drops and particles were oppositely charged. The improvement was more with submicron particles. For particles with diameters larger than 5 μm , charging the water and/or particles had little effect on efficiency.

Hook type nozzles gave a higher collection efficiency than the pigtail type. This could be due to smaller drops produced by hook type nozzles.

Experimental data were compared with predictions. For ND/UP, the measured single drop collection efficiency by Walton and Woolcock (1960) was used. For charged conditions, the theoretical single drop collection efficiency by Nielsen and Hill (1976) was used. For ND/UP conditions, the data agree with predictions. When either or both the particles and drops were charged, the measured collection efficiency was higher than that predicted. In fact the theory predicted that there should be no improvement in efficiency for the CD/UP and ND/CP conditions. This is contrary to experimental findings of the present study and those of others. Therefore, a new theory is required for the charged spray scrubber.

PROTOTYPE SCAT STUDY

A prototype SCAT scrubber system was designed and built. It consisted of two sections. One section housed the spray scrubber. The other section had three air curtains and one push fan. Both sections were mounted on casters; therefore, the distance between the air curtains and the spray scrubber could be adjusted. In operation, the two sections are arranged in a push-pull arrangement with the fugitive emission source situated in between.

The prototype SCAT was used for studying the effect of crosswind and the containment of a hot buoyant plume.

Crosswind Experiments

Under windy conditions, one can put the spray scrubber of the SCAT system downwind of the fugitive particle source. The wind will carry the particles to the spray scrubber where particles are removed from the air by water sprays. If the spray scrubber cannot be put downwind, the wind can be deflected from the FPE source with wind screens or air curtains. Fugitive particles are then conveyed to the spray scrubber with push jets. It is also possible to use one air curtain for both deflecting the wind and conveying the particles.

Crosswind experiments were carried out both indoors, and outdoors. In indoor experiments, the crosswind was simulated with another air curtain. The purpose of the indoor experiments was to determine what is required to deflect a crosswind and to verify published correlations. For the SCAT system, complete wind deflection is required to avoid disturbances in dust containment. Complete wind deflection is defined as the condition for which the resultant air flow of crosswind and SCAT air curtain jet flow bypassed the SCAT scrubber. Complete wind deflection occurs when the blocking distance or range produced by the SCAT air curtain is larger than the distance between the air curtain and the spray scrubber.

In order for the air curtain to deflect the wind, the momentum of the air curtain flow in the direction opposing the wind must be equal to or greater than the momentum of the wind. Wind deflection depends on several SCAT operating parameters such as the distance between the air curtain and the scrubber, air curtain slot width, slot exit velocity, the incident angle between the wind and air curtain jet stream, and the wind speed. The incident angle at which the air curtain meets the wind is the most important parameter. For maximum deflection, the jet stream should be at 45° opposing the wind.

Several formulae are available in the literature correlating the parameters mentioned above. Indoor experiments were intended for identifying the best correlation. Instead of fixing the incident angle at 135° , which gives the maximum range, experiments were performed by fixing the range, wind speed, air curtain slot width, and slot exit velocity and varying the incident angle for wind deflection. The measured angle was then compared with predictions. It was found that the measured angle agrees with that calculated from Abramovich's equation.

The use of an air curtain to simulate the crosswind has some shortcomings. It has a non-uniform velocity distribution and the wind direction does not change. In actual conditions, both the wind speed and direction fluctuate. For this reason, additional crosswind experiments were done outdoors.

Since an air curtain could be used for deflecting a crosswind only or could be used for deflecting a crosswind and conveying the dust at the same time, three experiments were done.

The first experiment was for determining the air curtain range. The spray and air curtain were so arranged that the wind direction was perpendicular to the spray scrubber and the air curtain jet discharged at 45° against the wind. Smoke or flyash particles were injected at various locations for flow pattern observations. If the smoke rises vertically, the wind is deflected from that location. Otherwise, it is not being deflected. The air curtain range is the distance from the air curtain to the nearest location where the wind is not being deflected.

The jet stream of an air curtain is curved in the presence of crosswind. The second experiment determined the jet stream trajectory for various wind speed, incident angle, air curtain slot width, and exit velocity. A constant temperature thermal anemometer was used for locating the trajectory of the jet stream centerline.

If the air curtain is also for conveying the dust, then one needs to rotate the spray scrubber to intercept the curved jet stream. The third experiment involved the measurement of the angle of rotation for various crosswind and wind/air curtain incident angles.

Measured air curtain range, trajectory, and rotation angle of the spray scrubber to intercept the jet stream were compared with predictions from Abramovich's equation. The agreement is good. Therefore, in operating the SCAT system under crosswind conditions, this equation can be used for locating the air curtain and spray scrubber relative to the crosswind and fugitive emission source.

Hot Source Experiments

In some metallurgical processes, such as iron and steel manufacturing processes, very hot plumes containing very high concentrations of particles

are released. The most efficient and economical way in cleaning these plumes would be to capture them at the source where the concentration is highest and gas volume smallest. However, in many cases for practical reasons, such as the presence of overhead cranes, it is impossible to capture the plume at the source or even vertically above the source with fume hoods. In these situations a SCAT system could be applied to control the emissions. An air curtain could be used as the ceiling to contain the fumes and dust and horizontally displace the plume into a receiving hood or directly into a scrubber if space is available for its installation.

Experiments were done to study the feasibility of containing hot plumes with air curtains. The hot source was simulated with an open top furnace which had a natural gas open flame burner. The furnace was a rectangular box lined with insulating fire-bricks. There was an opening on top of the furnace for the hot gas to exit.

The furnace was located at the center of the SCAT system. The distance between the air curtain assembly and the spray scrubber was 3.1 m. Since the operation of the burner was fixed, the ceiling air curtain location was adjustable so that the jet stream could meet the hot plume at different temperatures.

Most of the experiments were performed with the ceiling air curtain exit axis located at 61 cm (2 ft.) above the top of the furnace. The peak plume rising velocity and temperature at this location were about 190 cm/s (4.3 mph) and 471°C (880°F), respectively.

Experiments were done for three air curtain slot widths and four slot exit velocities. Except for small slot width (2.5 cm or less) coupled with low exit velocity (20 m/s or less), the air curtain was capable of containing the hot plume. In general, small air curtain slot width needed high exit velocity for containment. At low air curtain slot exit velocities, the hot plume mixed well with the air curtain flow vertically, i.e. penetration of hot plume into air curtain jet stream was high. There was little mixing in the horizontal direction. As the slot exit velocities was increased, the vertical mixing decreased while horizontal mixing increased.

A correlation was derived for predicting the hot plume trajectory. It was derived from a momentum balance taking into account the buoyancy and momentum of the plume. Predictions by this equation correlate well with data.

CONCLUSIONS

A simple technique for controlling fugitive process emissions has been developed. The technique involves the use of air curtains and air jets to contain and convey the emissions into a nearby spray scrubber.

The collection efficiency of a spray scrubber was investigated experimentally. The collection efficiency could be improved by charging the water and/or the particles. The measured particle penetration agrees with theoretical predictions for the un-augmented scrubber. For the electrostatically augmented scrubber, the measured penetration is lower than that predicted.

Air curtains have been used in industry to contain dust but no carefully performed study has been reported in the literature. The air curtain developed in the present study can achieve a smaller expansion angle and a lower entrainment ratio than those reported in the literature. Small expansion angles and entrainment ratios are beneficial to the control of fugitive process emissions with the SCAT system.

A prototype SCAT has been built to study the effects of crosswind and containment of hot buoyant plume. It has been shown that the equation of Abramovich gave reasonable predictions on the range of air curtains, trajectory of air curtain axis in the presence of crosswind, and the required spray scrubber rotation angle for intercepting the jet stream. Therefore, in operating the SCAT system under crosswind conditions Abramovich's equation can be used for locating the air curtain and spray scrubber relative to the crosswind and emission source.

The air curtain was successful in containing a hot buoyant plume. At an air curtain/spray scrubber separation of 3 m, an air curtain with slot width of 5.1 cm and air exit velocity of 20 m/s can contain a hot plume which is

rising at a velocity of 200 cm/s and has a temperature of 470°C. The trajectory of the plume can be predicted from plume rise formulas.

We have done most of the basic research in the present study. Since the SCAT system design is source specific, the next step would be to choose a source and site and build a SCAT for that site to demonstrate the feasibility of the SCAT system. In doing so, some additional studies of specific configurations may be needed.

SECTION 2

INTRODUCTION

Fugitive emissions constitute a large percentage of total particulate emissions. Therefore, control of fugitive emissions will be an essential ingredient in control strategies for particles.

Fugitive emissions are classified into two categories; fugitive dust emissions and industrial fugitive process emissions. Fugitive dust emissions originate from non-industrial activities, such as wind blown dust, entrained street dust, agriculture, and construction and demolition. Fugitive process emissions are any non-ducted emissions due to industrial activities such as loading and unloading raw materials, unpaved road in a plant, metal pouring, material charging, etc.

Carpenter and Weant (1978) reviewed the present control strategies for fugitive emissions. They concluded the present strategies are inadequate and their effectiveness is highly variable from one site to another.

Some degree of fugitive particle control can be obtained with little or no equipment investment via good housekeeping and plant maintenance practices. Prompt repair of hood damage, maintenance of seals on coke oven doors, proper handling and disposal of dusts from fabric filters, quick cleanup and disposal of particulate spills, and any of the numerous other elements of good manufacturing practice all serve to reduce the amount of fugitive particulate emissions. This type of control depends on work practices.

For open source fugitive particulate emissions, such as storage piles, unpaved roads, transfer points, etc., the most rudimentary control method involves stabilizing the dust with water spray to keep it from dispersing. Spraying with water generally requires a low first cost, but provides only temporary dust control. The nature of the dust producing activity determines whether water sprays will give effective stabilization for only a few hours or for several days.

Several types of chemicals have been found to be effective in reducing fugitive dust emissions when applied to the source. Over 100 chemical products are presently marketed or under development specifically as dust control agents. These chemicals act by several different means and are generally categorized by their composition; bituminous, polymer, resin, enzymatic, emulsion, surface-active agent, ligninsulfonate, latex, etc.

A permanent way to control fugitive process emissions is to gather them and convey them to conventional particle removing devices. Emissions from a source are gathered and transferred to the control device via a ventilation system before particles are removed. Ventilation systems for fugitive particles are typically either secondary hooding at the local source of emissions, or total building enclosure and evacuation.

High energy and capital investment are required in controlling fugitive emissions by these methods, especially when there are many small, diffuse emission sources. In addition, secondary hooding may not be efficient in gathering the emissions. For example, a Pierce-Smith convertor in a copper smelter equipped with fixed or secondary hooding will capture at the most a small percentage of the fugitive emissions created during the charging of matte, slagging, or blister copper pouring (Craig, et al. 1979).

A much simpler and more cost effective method for controlling fugitive emissions is by diverting the emissions into a control device located near the source. The SCAT scrubber (Spray Charging and Trapping Scrubber) is such a system. It uses air curtains and/or air push jets for gathering and conveying the emissions into a scrubber.

This report presents research results on air curtain design, charged spray scrubber performance, the deflection of cross-wind and the containment of hot buoyant plumes with air curtains. An example of using the SCAT system for controlling the emissions from a coke oven pushing operation is also presented.

SECTION 3 SCAT SYSTEM

The overall effectiveness of controlled disposal of fugitive particle emissions (FPEs) is determined by how well the system gathers FPEs as well as by how efficiently it collects what it gathers. The SCAT scrubber system uses air curtains and/or air jet to contain, convey, and divert the FPEs into a charged spray scrubber which is near the source.

Figure 1 shows an example of the SCAT. The air curtain (and/or air jets) and the spray scrubber are arranged in a "push-pull" fashion with the fugitive particle emission source situated in between.

An air curtain involves the use of one or more high velocity air streams flowing as a sheet. The air sheets are produced by one or more air jets which issue from circular, or rectangular nozzles. The high velocity air streams will push and entrain FPEs plus some additional air and will carry them away from the source. At some convenient distance downstream, charged water is sprayed concurrently into the gas stream to remove the entrained dust. After sufficient contacting distance to effect capture of the particles present in the gas, water spray drops are removed with a low pressure drop entrainment separator. Either a parallel plate or a zigzag baffle type entrainment separator may be used, depending on mist elimination and pressure drop requirements.

The water from the entrainment separator can be passed through a separation process, such as a filter, to remove the collected particles. The water may then be recycled and the particles may be disposed of in such a way as to prevent their redispersion. As an alternative, a blow-down stream of dirty liquid may be directed to a disposal system.

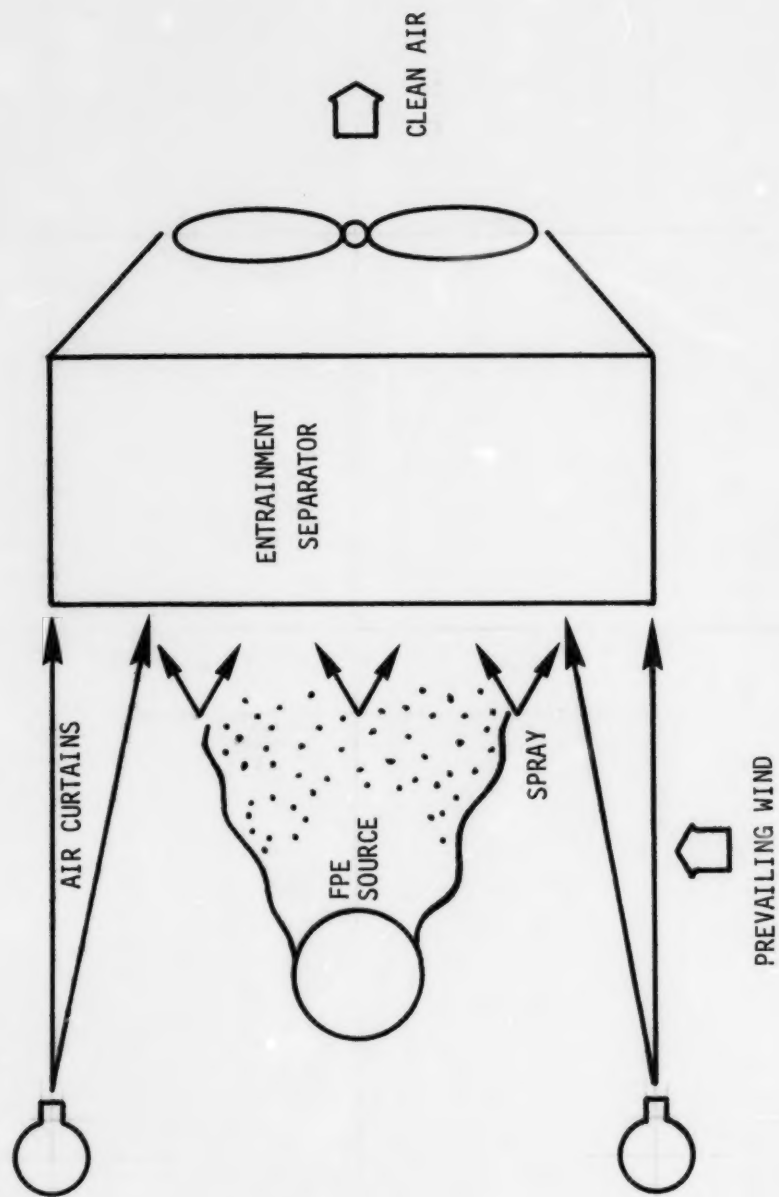


Figure 1. Example of SCAT control system.

The SCAT system has the following features which suit it to fugitive process emission control:

1. Minimum use of solid boundaries enabling access to the source.
2. Air curtains and/or air push jets to contain, divert, and convey the fugitive particles. Minimum use of duct work or hooding.
3. Can deflect crosswind.
4. Can contain hot buoyant plume.
5. Unobtrusive.
6. Portable.
7. Traps particles and removes rather than depositing them at the source site.

The SCAT scrubber system could have four major components: (1) air curtains or air push jets, (2) charged water sprays, (3) entrainment separator, and (4) water treatment and recycling system. There is no single design or configuration for the SCAT system and it is not necessary for it to have all four components. Its design may vary from source to source. Figures 2 and 3 show some of the many possible configurations.

When there is wind and the FPE source is outside the building, the simplest control technique is to put the sprays downwind of the source. If the wind is strong enough, air curtains or air push jets are not required to divert the particles. In situations where putting the SCAT downwind is not possible, portable solid boundaries or air curtains can be used to deflect the wind and air push jets and air curtains to divert the FPEs into the spray zone. If the distance between the air curtain nozzles and the spray zone is more than 6.1 m (20 feet), booster jets can be used to help move the FPEs.

Windy conditions are expected to be encountered in the following sources and SCAT could be applied:

1. Dump site, e.g., asbestos.
2. Storage piles.
3. Loading and unloading of raw materials from storage piles.

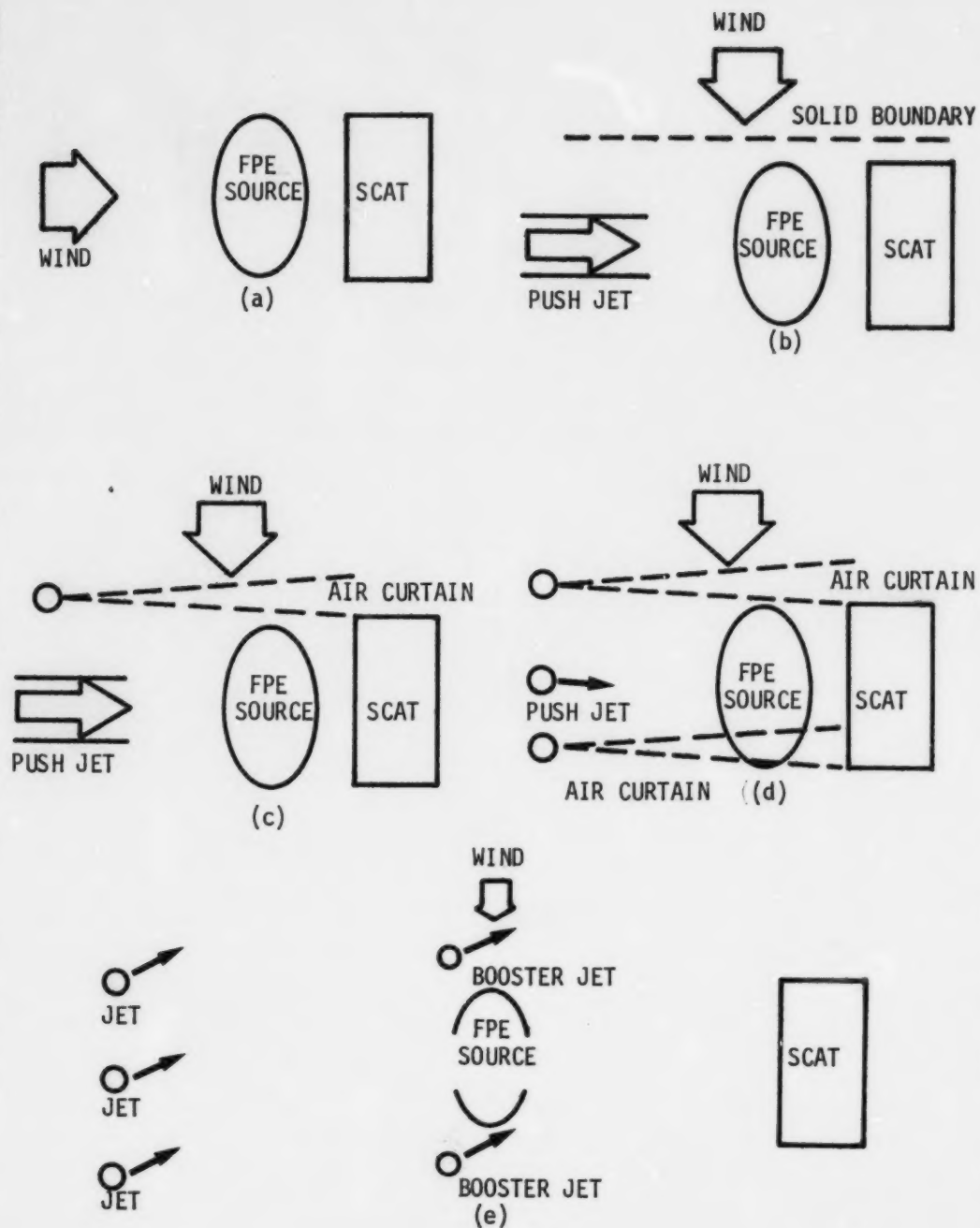


Figure 2. Possible SCAT designs, windy conditions.

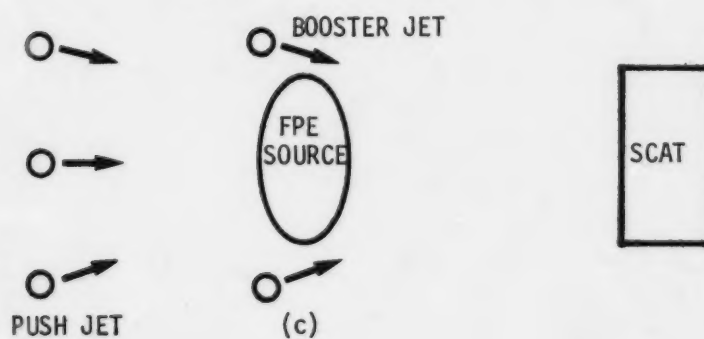
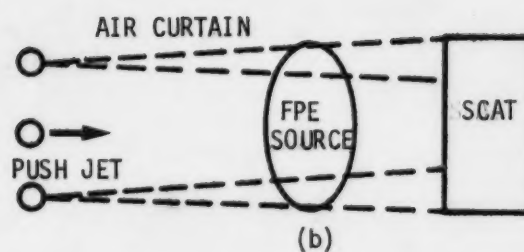
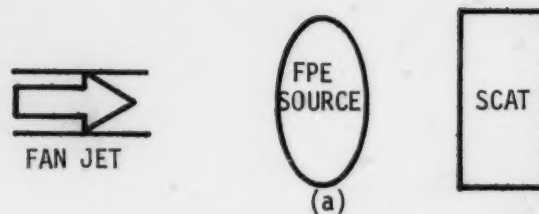


Figure 3. Possible SCAT scrubber designs, calm conditions.

4. Loading and unloading of raw materials from trucks, e.g., mining.
5. Sand, gravel, and asphalt batching.
6. Coke oven pushing operation.
7. Transfer points on conveyor belts.

Under calm conditions, such as inside a building, push jets can be used to divert the FPEs into the sprays. Air curtains could also be used to help channel the FPEs and to separate the working area from the source.

Calm conditions occur in the following industrial operations:

1. Metal pouring,
2. Molding,
3. Mold preparation, and
4. Charging of raw materials into furnaces, roasters, and convertors.

SECTION 4

AIR CURTAIN STUDY

The SCAT scrubber system has three important functions: (1) contain and convey the fugitive emissions to the spray zone with air curtains, (2) collect the particles with charged water sprays, and (3) remove the water drops.

There is little published information on the characteristics of air curtains and the design of charged spray scrubbers. To generate design information quickly, air curtains and the charged spray scrubbers were first studied individually in separate bench scale devices and then combined into one unit.

An air curtain is a sheet of air blown out of nozzles of circular or rectangular section at high speed. The principle of the air curtain was first applied in 1904 by Theophilus van Kemmel (Herndon, 1966). He took out a patent to seal an entrance from outside weather. Since 1916, the device has been further developed and perfected. Two main types of air curtains exist today: with recirculation, i.e., with a return duct arranged in push-pull configuration, and without. In either case, the flow may be vertical or horizontal. But each type may also vary in type of fan, jet velocity, depth, and direction of curtain.

Today, simple to elaborate air curtains up to 27.4 m (90 ft) wide and 4.6 m (15 ft) high are widely used in industrial surface plants, mainly to provide constant access or to isolate a warm interior from the cold doors or vice versa. Air curtains can also be used to contain and convey dusty gases. For example, a non-recirculating horizontal air curtain is used at the Quebec Iron and Titanium Smelter in Sorel to separate two ambients: one building in which molten slag cars are quenched with water (which produces large volumes of steam and fumes) from the main working area (Grassmuck, 1969). The total air volume handled is $2,720 \text{ m}^3/\text{min}$ (9,600 CFM) over a great width but at normal height. At Naoshima Island, Japan, the Naoshima Smelter of Mitsubishi metal Corporation uses an air curtain system to collect and convey the fugitive emissions from a copper converter into a hood (Uchida et al. 1979).

The design of the SCAT system requires information on several parameters, of the jet stream. They are the jet expansion angle, air entrainment ratio, mixing of particles in the curtain, effect of crosswind, and the effect of hot sources. The jet expansion angle and particle mixing determine the overall cross-sectional dimensions of the SCAT system; the air entrainment ratio determines the volumetric flow rate; and the crosswind and heat effects dictate the placement of air curtains and sprays.

Air curtain flow fields (jet expansion angle, velocity distribution, and air entrainment ratio) were studied in an auxiliary air curtain study. The effects of crosswind and hot source were studied with the prototype SCAT scrubber.

LITERATURE REVIEW

Air curtains have been widely used for air conditioning and ventilation. At doorways where the solid door is open, either continuously or intermittently, an air curtain could be installed to retard the flow of heat and moisture between the outside and the enclosed space and to deflect the outside air away from the door. Most of the published literature, e.g., Croome-Gale and Roberts (1975), Baturin, (1972), and Hayes and Stoecker (1969a, b) are concerned about the heat transfer through the curtain and the requirements for it to block the outside wind.

An air curtain approximates a two-dimensional free jet and there is much information, both theoretical and experimental, available in the literature. A brief review of air curtain manifold design and theories on turbulent free jet is presented in the following sections.

Air Curtain Manifold Design

An air curtain distribution manifold is a dead-end duct with side outlets. The duct could be of uniform cross-section or tapered. The side outlets could be a continuous slot or uniformly spaced nozzles.

For an air curtain to perform properly, the manifold should give uniform distribution of discharge along the manifold length. In addition, the discharge direction should be as close to perpendicular to the flow direction in the manifold as possible. The manifold problem has been studied by a number of researchers. Koestel and Tuve (1948) and Koestel and Young (1951) calculated and measured the outflow from a constant area and a tapered, slotted duct. Keller (1949) and Dow (1951) applied their calculations and tests to get even flame length along gas burners. Haerter (1963) surveyed the research results and gave design formulas and data.

There are two important factors which determine the distribution of flow in and from a manifold; these are inertia and friction. Inertia corresponds to change of velocity head. In general, as the fluid flows along the manifold its longitudinal velocity decreases due to part of the fluid being discharged laterally through the openings. Therefore, the fluid in the manifold is being decelerated and, in accordance with Bernoulli's principle, this tends to increase the fluid static pressure. This is termed static regain. Friction, on the other hand, results in loss of pressure along the length. The relative magnitudes of the pressure regain due to deceleration and the pressure loss due to friction determine whether the pressure rises or falls from the inlet end to the dead end of the manifold. In cases where the cross section of the manifold can be varied, it is usually possible to proportion the area along the length so that the two opposing factors just balance and the pressure remains constant at all points, resulting in uniform discharge per unit length.

Three basic equations are necessary to describe the outflow from a manifold. For simplicity, the flow is assumed to be one-dimensional. A momentum balance on the control volume shown in Figure 4 yields the following differential equation for static pressure change along the duct.

$$\frac{dp}{dx} = -\frac{f \rho_G}{2g d_h} u_G^2 - \frac{\rho_G}{2g} \left(2 - \frac{u_{GW}}{u_G} \right) \frac{du_G^2}{dx} - \frac{\rho_G u_G}{gA} \left(1 - \left(\frac{u_{GW}}{u_G} \right) \frac{dA}{dx} \right) \quad (1)$$

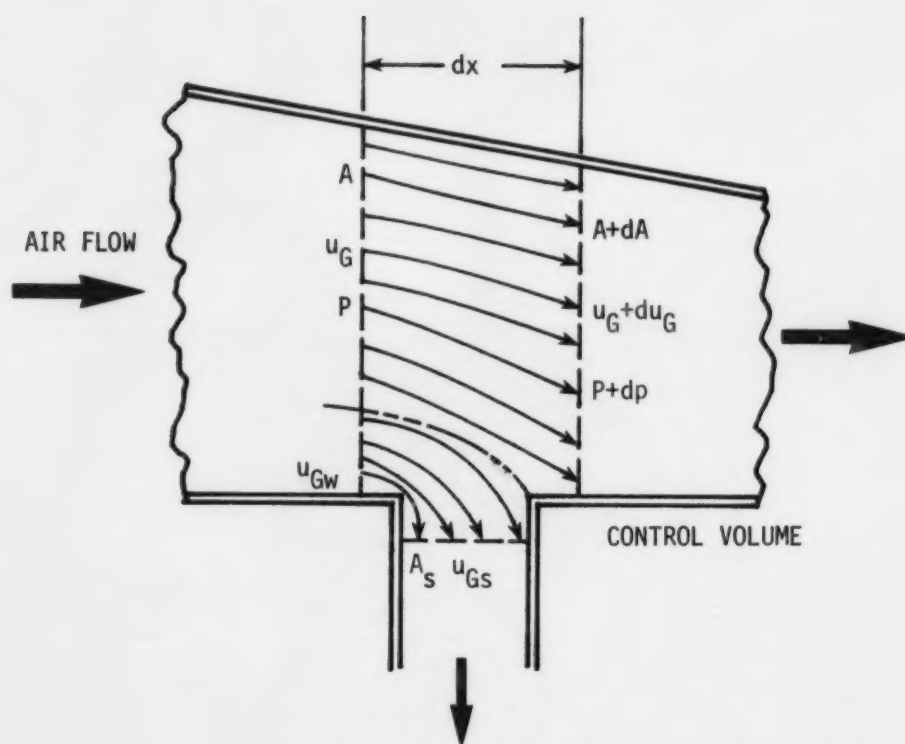


Figure 4. Section of air curtain manifold.

where p = average static pressure in the duct at a cross-section, Pa
 f = friction factor, dimensionless
 d_h = hydraulic diameter of the duct, cm
 u_G = average linear velocity in the duct, cm/s
 A = cross-sectional area of the duct, cm²
 u_{GW} = average velocity of the flow material near the wall which will be branched off through the side opening, cm/s
 x = distance from the start of the side opening, cm
 g = constant of gravity, 980 cm/s²
 ρ_G = gas density, g/cm³

" u_{GW} " stands for the mean velocity of the flow material near the wall, which will be branched off through the side opening. " u_{GW} " is smaller or larger than " u_G " depending on the location along the duct. If the flow is plug flow in the duct, then " u_{GW} " is equal to " u_G " and equation (1) becomes:

$$\frac{dp}{dx} = - \frac{f \rho_G}{2 g d_h} u_G^2 - \frac{\rho_G}{2g} \frac{du_G^2}{dx} \quad (2)$$

The term " $2 - u_{GW}/u_G$ " in equation (1) is defined as the pressure regain coefficient.

$$R = 2 - \frac{u_{GW}}{u_G} \quad (3)$$

Where R = static regain coefficient, dimensionless.

Accurate " R " values have been determined by Ashley, et al., (1956). Figure 5 shows the measured static pressure recovery coefficient along a manifold with even outflow distribution.

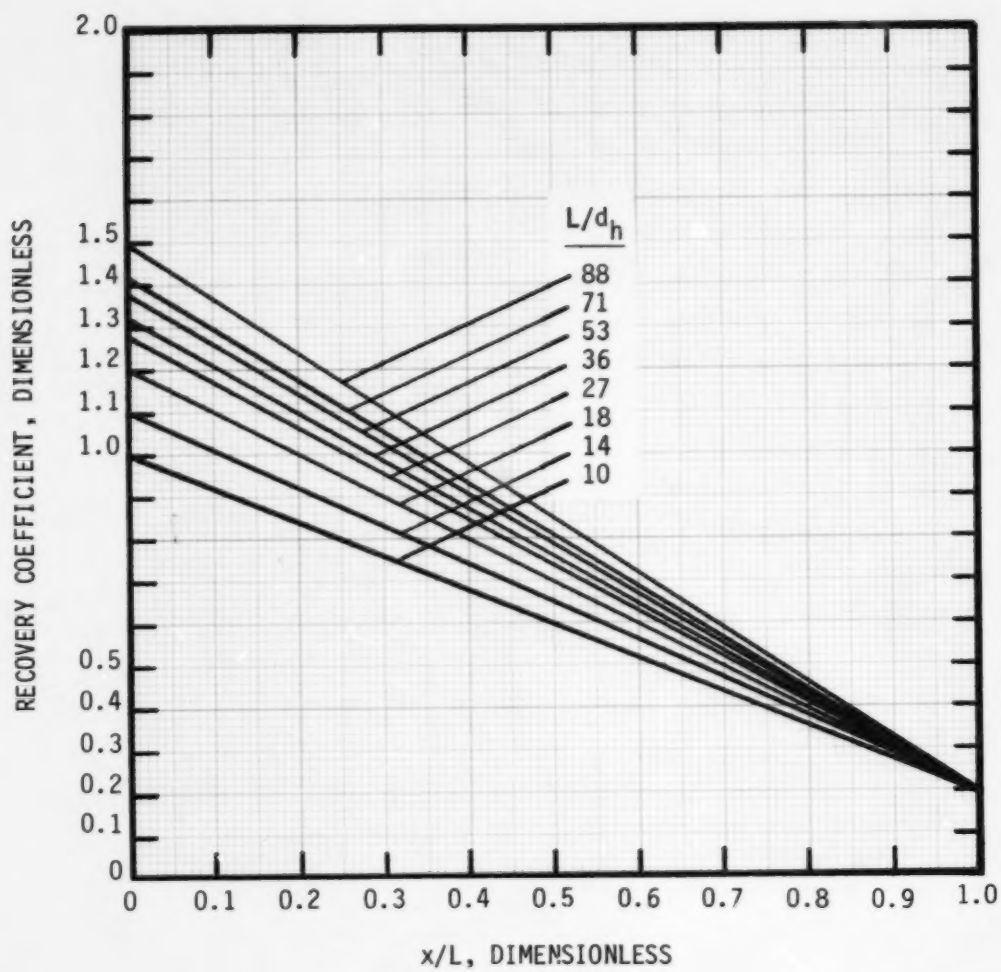


Figure 5. Measured static pressure recovery coefficients R along a supply air duct with even outflow distribution.

The pressure regain coefficient "R" decreases along the duct due to an increasingly unsymmetrical velocity profile with the peak velocity shifting toward the side opening. For manifold design, the decrease can be assumed linear and the average regain coefficient " $\bar{R}$ " calculated from the following formula can be used for the whole duct:

$$\bar{R} = R_0 - \frac{1}{3} (R_0 - R_1) = R_0 - \frac{1}{3} (R_0 - 0.2) \quad (4)$$

Where $\bar{R}$ = average pressure regain coefficient, dimensionless

R_0 = pressure regain coefficient at the beginning of the side outlet, dimensionless

R_1 = pressure regain coefficient at the end of the side outlet, dimensionless

The second basic equation is derived from the equation of continuity. The volumetric flow in the manifold is decreased by the amount of the branch flow, i.e.

$$\frac{d(Au_G)}{dx} = u_{Gs} \frac{dA_s}{dx} \quad (5)$$

Where A = manifold cross-sectional area, cm^2

A_s = side outlet cross-sectional area, cm^2

u_G = gas velocity in the manifold, cm/s

u_{Gs} = average side outlet velocity, cm/s

x = distance from the beginning of the side outlet, cm

In the case of a continuous slot, " dA_s/dx " stands for the slot width at " x ", whereas in the case of individual side openings " dA_s/dx " equals the total side opening area divided by the duct length.

Finally, the average side outlet velocity, " u_{Gs} ", can be calculated with the help of the total pressure balance along the branch stream.

$$p + \frac{\rho_G}{2g} u_G^2 = \frac{\rho_G}{2g} u_{Gs}^2 + \lambda_s \frac{\rho_G}{2g} u_{Gs}^2 \quad (6)$$

where p = average static pressure in the manifold, Pa
 u_G = average velocity in the manifold, cm/s
 u_{Gs} = average side outlet velocity, cm/s
 ρ_G = gas density, g/cm³
 g = constant of gravity, cm²/s
 λ_s = loss coefficient for the diverted flow, dimensionless

The loss coefficient for the diverted flow section is identical with the one established on a single branch fitting. For rectangular takeoffs, it has been shown (Korst, et al. 1950; Holl, et al., 1951; Gilman, 1955; Ashley, et al., 1956; Miller, et al., 1956) that the turning losses may be split easily into a portion depending on the manifold dynamic pressure and the rest depending on the side outlet dynamic pressure so that equation (6) can be written as:

$$p + \frac{\rho_G}{2g} u_G^2 - \lambda_m \frac{\rho_G}{2g} u_G^2 = \frac{\rho_G}{2g} u_{Gs}^2 + \lambda_s \frac{\rho_G}{2g} u_{Gs}^2 \quad (7)$$

Where λ_m = loss coefficient for the manifold, dimensionless
 λ_s = loss coefficient for side outlet, dimensionless

Loss coefficients are defined as:

$$\lambda_m = \frac{H_m}{u_G^2 / 2g} \quad (8)$$

$$\lambda_s = \frac{H_s}{u_{Gs}^2 / 2g} \quad (9)$$

Where H_m = loss of pressure head in the manifold, cm W.C.

H_s = loss of pressure head in the side outlet, cm W.C.

Gilman (1955) gave the following equations for estimating the pressure head losses for right angle junction:

$$H_m = 0.35 (u_G - u_{Gs})^2 / 2g \quad (10)$$

$$H_s = 0.5 \frac{u_{Gs}^2}{2g_c} + \left(\frac{u_G}{u_{Gs}} \right)^2 \quad (11)$$

The above equations are the governing equations for designing a manifold with even flow distribution. A discussion on applying the above equations to design an air curtain manifold is presented in the following sections. Since air curtain manifolds are relatively short ($L/d_h < 50$), the discussions are limited to short ducts.

Manifold With Constant Cross-Section and Constant Side Outlet Configuration

For a short duct, the friction loss term can be neglected and the total pressure remains constant along the duct. Combining equations (1), (5), and (6) and introducing the following dimensionless variables,

$$x^* = \frac{x}{L}, \quad u_G^* = \frac{u_G}{u_{Go}}, \quad p^* = \frac{p}{\frac{\rho_G u_{Go}^2}{2g}} \quad (12)$$

a second order differential equation is obtained

$$\frac{d^2 u_G^*}{dx^{*2}} + \left[\frac{A_s}{A (1 + \lambda_s)^{0.5}} \right]^2 R \lambda_m u_G^* = 0 \quad (13)$$

Where A = manifold cross-sectional area, cm^2
 A_s = side outlet flow area, cm^2
 u_{Go} = gas velocity at entrance of the manifold, cm/s
 L = length of the slot, cm
 $\bar{R}$ = average pressure regain coefficient, dimensionless
 λ_s = loss coefficient for side outlet, dimensionless
 λ_m = loss coefficient for the manifold, dimensionless

Haerter (1962) solved equation (13) to obtain

$$\frac{Q_{Gs}}{\bar{Q}_{Gs}} = \frac{C_1 C_2}{\sin(C_1 C_2)} \cos \left[C_1 C_2 \left(1 - \frac{x}{L} \right) \right] \quad (14)$$

Where Q_{Gs} = volumetric flow rate per cm of slot length or per nozzle cm^3/s
 $\bar{Q}_{Gs}$ = average volumetric flow rate for the whole length of slot, cm^3/s
 $C_1 = \frac{A_s}{A (1 + \lambda_s)^{0.5}}$, dimensionless
 $C_2 = (R \lambda_m)^{0.5}$, dimensionless
 L = slot or side outlet length, cm
 x = distance from beginning of the slot, cm

The static pressure at the manifold entrance can be calculated from the expression

$$p_o = \frac{\rho_G u_{Go}^2}{2g} \left[\lambda_m (1 - \bar{R}) - 1 + \frac{C_2^2}{\sin^2(C_1 C_2)} \right] \quad (15)$$

Where p_o = static pressure at manifold entrance, cm W.C.

"C₁" is a parameter dealing with static pressure and is equal to the ratio between the effective area of the side outlet and the manifold cross-sectional area. "C₂" expresses the portion of pressure head recovery along the duct and loss at the side outlets. The behavior of the manifold is determined by the magnitude of these two parameters. Each pair of "C₁" and "C₂" determines the aerodynamic behavior of the distribution system. Figure 6 shows a plot of relative flow rate, " $Q_{Gs}/\bar{Q}_{Gs}$ ", versus dimensionless distance with "C₁C₂" as the parameter. It can be seen from this figure that to obtain uniform distribution, C₁C₂ < 0.25. For a side outlet joined with sharp edges to the manifold, $\lambda_m \approx 1$ and $\lambda_s \approx 0.5$. $\bar{R}$ is about 0.7. Using these approximated values, the total side outlet area should be no more than 37 percent of the manifold cross-sectional area.

Tapered Manifold with Constant Side Outlet Configuration

Where the cross-sectional area of the manifold can be varied along its length, the contour can usually be so proportional that friction loss exactly counterbalances static regain at every point. Thus, the pressure can be caused to remain constant, and the discharge uniform, along the length.

For a tapered duct, the velocity profile in the manifold can be assumed uniform, i.e., $u_{GW} = u_G$. If the manifold is short ($L/d_h < 10$) friction loss could be neglected. Then, according to equation (2), the longitudinal velocity in the manifold should be kept at a constant for uniform discharge. This could be done by decreasing the cross-sectional area of the manifold linearly from the maximum at the inlet end to zero at the dead end.

If friction loss is appreciable, the contour should be curvilinear in shape. If changes of friction factor along the duct is small, an average value for "f" could be used and equation (2) becomes

$$\frac{du_G}{dx} = - \frac{fu_G}{2d_h} \quad (16)$$

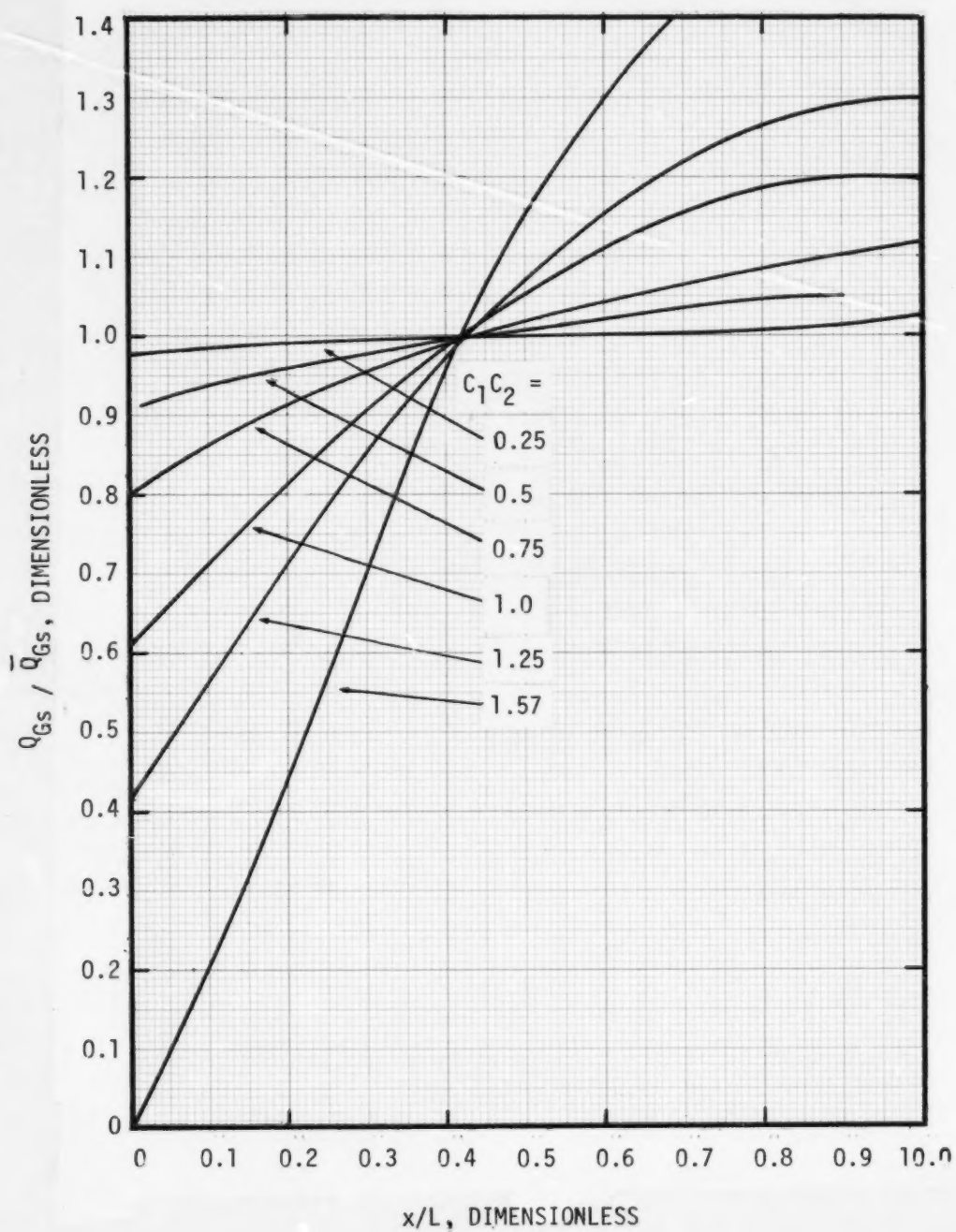


Figure 6. Distributions of outflowing air along relatively short supply air ducts with constant cross-section.

Where u_G = longitudinal gas velocity in the manifold, cm/s
 f = friction factor, dimensionless
 d_h = hydraulic diameter of the manifold at location "x", cm
 x = distance from the start of the slot, cm

Since the discharge is to be uniform per unit length, the volumetric flow rate in the manifold decreases linearly from " Q_{Go} " to zero at the dead end

$$u_G = \frac{Q_{Go}}{A} \left(1 - \frac{x}{L}\right) \quad (17)$$

Where u_G = longitudinal gas velocity in the manifold, cm/s
 Q_{Go} = volumetric flow rate at manifold entrance, cm³/s
 A = cross-sectional area of the manifold at "x", cm²
 L = length of side outlet, cm
 x = distance from the start of the side outlet, cm

Equations (16) and (17) coupled with the expression for " A " and " d_h " can be applied to design a manifold of variable cross section to give uniform distribution of discharge. For example, for rectangular cross section of uniform width and varying height

$$A = bh \quad (18)$$

$$d_h = \frac{2bh}{b+h} \quad (19)$$

Where A = cross-sectional area of the rectangular manifold, cm²
 b = width of the manifold, cm
 h = height of the manifold, cm

Substituting equations (17) through (19) into equation (16), a first order differential equation is obtained and the solution to that equation is

$$h = (L-x) \exp \left\{ \left[-\frac{f(L-x)}{4b} \right] \left[C_3 - \frac{f}{4b} \ln(L-x) + \frac{f(L-x)}{4b} + \frac{1}{4} \frac{f(L-x)^2}{4b} + \dots \right] \right\} \quad (20)$$

Where h = height of the rectangular manifold at "x", cm

L = length of the slot,

x = distance from the beginning of the slot, cm

b = width of the manifold, cm

f = friction factor, dimensionless

C^3 = integration constant

The integration constant, " C_3 ", can be determined from the boundary condition at $x = 0$, $h = h_0$. Figure 7 shows a plot of the contours for various manifold lengths. As can be seen, when $L/d < 10$, deviation from the no friction case is small and the sloping plane can be straight.

Discharge Angle

A good air curtain requires that the air discharge angle be uniform from end to end and preferably at 90° with the manifold axis. This poses no design problem if nozzles are attached to manifold as side outlets. If the side outlet is a long slot or drilled holes, the air will discharge at an acute angle with the axis of the manifold unless guide vanes are used.

The angular direction of the discharge air stream with respect to the slot axis is related to a vectorial sum of two velocities as shown in Figure 8. If " u_G " is the longitudinal velocity in the manifold and $C_d(2gp)^{0.5}$ is a velocity perpendicular to the slot face due to the conversion of manifold static pressure through the slot, the slot discharge velocity will be the resultant of these two velocities.

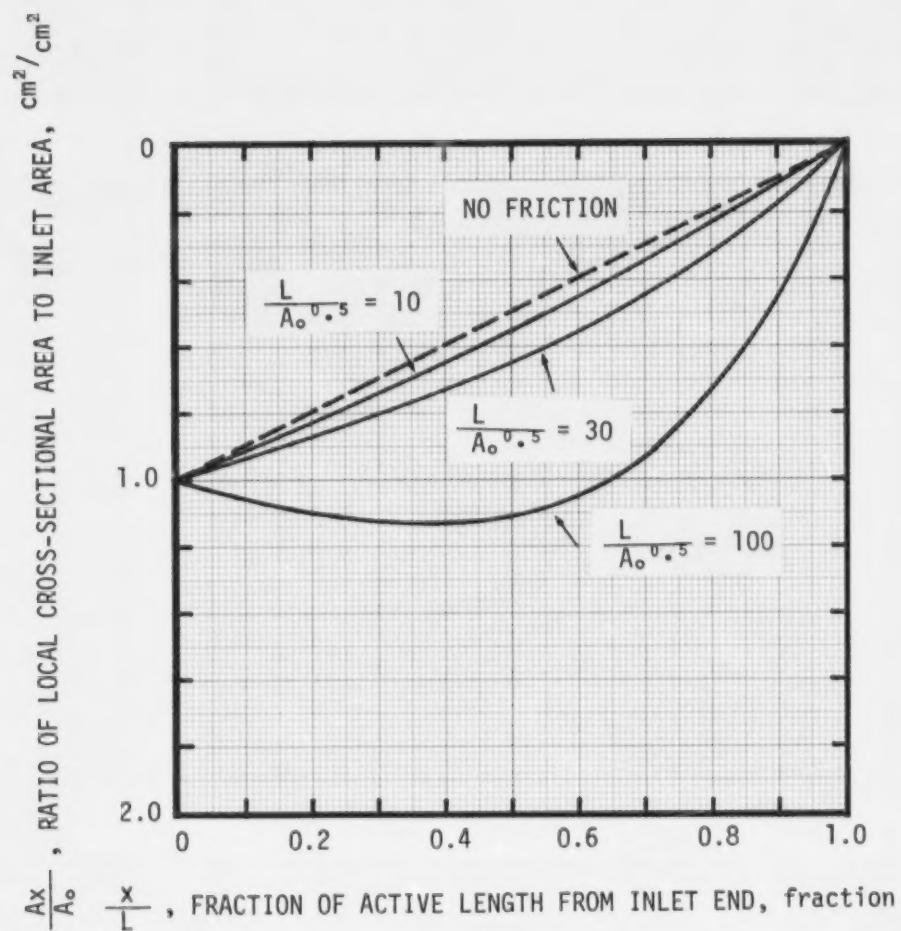


Figure 7. Contours of manifold to give uniform discharge distribution.

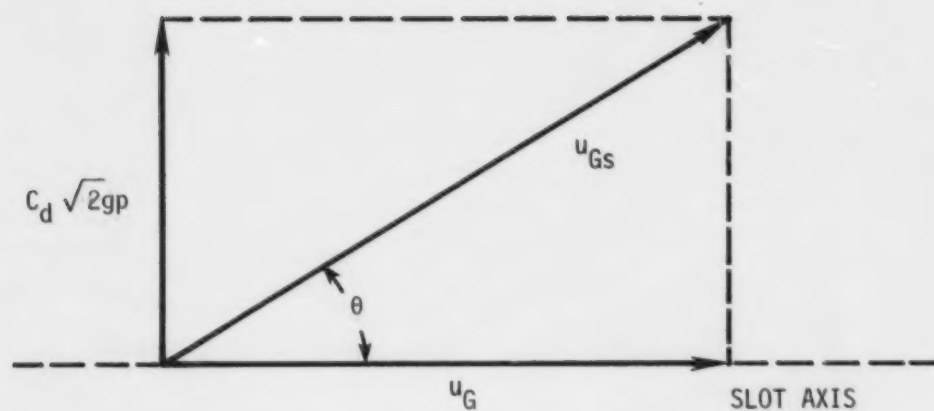


Figure 8. Velocity vector diagram.

$$u_{Gs}^2 = 2gpC_d^2 + u_G^2 \quad (21)$$

Where u_{Gs} = slot discharge velocity, cm/s
 C_d = slot discharge coefficient, fraction
 p = manifold static pressure, dyne/cm
 u_G = longitudinal velocity in the manifold, cm/s

The discharge angle, " θ ", is calculated from

$$\theta = \tan^{-1} \left(\frac{C_d (2g p)^{0.5}}{u_G} \right) \quad (22)$$

For a manifold with constant cross section, since both the static pressure and the manifold velocity change from end to end, the discharge angle changes from end to end. The angle is smaller at the start of the slot due to a higher manifold gas velocity.

Koestel and Tuve (1948) measured the air discharge angle for manifolds with rounded entrance slots and square edged slots. Figures 9 and 10 show their experimental results. The air discharge angle along the slot was found to vary in a straight line and these angles were dependent on the slot/manifold area ratio and independent of the air velocity.

For short tapered duct, static pressure and gas velocity are constant. Therefore, the air discharge angle will be constant along the slot and can be calculated from the following equation

$$\cot \theta = \frac{A_s C_d}{A} \quad (23)$$

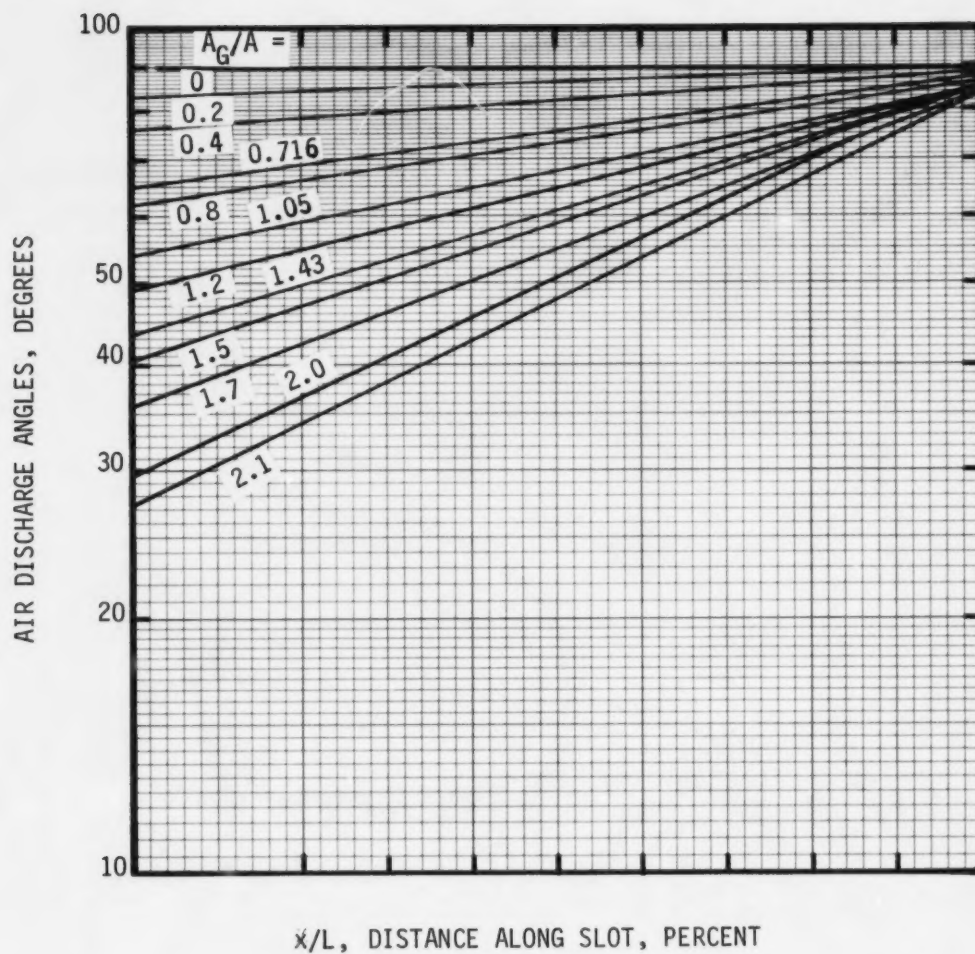


Figure 9. Measured air discharge angle for square edged slots.

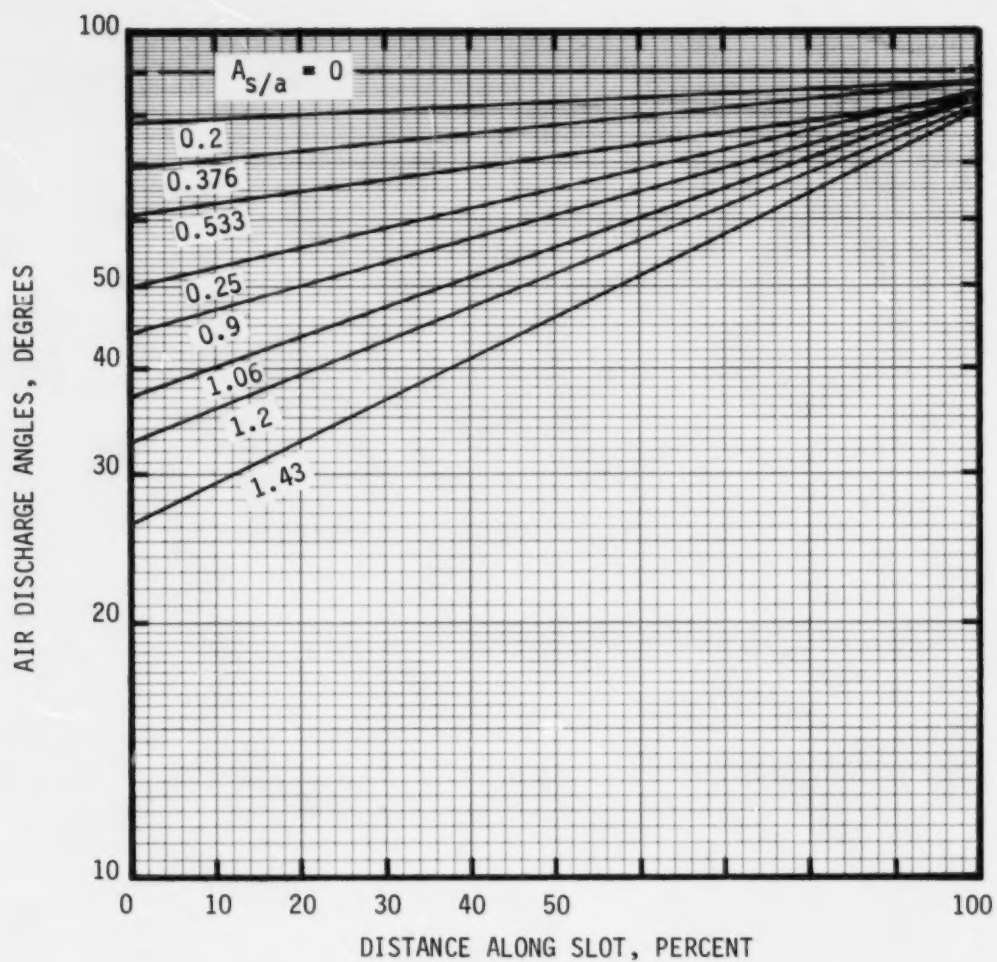


Figure 10. Measured air discharge angle for rounded-entrance slots.

Where θ = discharge angle, degrees

A = cross-sectional area of the manifold at "x", cm^2

A_s = side outlet flow area, cm^2

Equation (23) was verified experimentally by Koestel and Young (1951).

To obtain an air discharge angle perpendicular to the manifold axis, turning vanes or deflectors should be applied. A short tapered manifold is best suited for turning vanes due to the fact that the discharge angles remain constant. This feature would make the use of turning vanes along the outside surface of the slotted orifice more practical from the standpoint of design and construction. This would not be possible with a constant cross-section manifold since each vane would have to be designed to meet the condition of flow at a particular point along the slot which is variable due to static regain.

The amount of turning required of a vane would depend on the discharge angle. Deflecting vanes should be located on the outside surface of the slot since the direction of the emanating air stream is determined after the manifold static head is converted into velocity through the slotted orifice.

A turning vane, if designed correctly, should have the shape of an air foil so that separation of the deflected air stream from the vane surface does not take place. This would be impractical and uneconomical in an actual air curtain. Vanes of single thickness are much more easily fabricated than vanes of variable thickness as in an air foil. In order for a guide vane of single thickness and a given height to be efficient, the curvature should be as flat as possible without any abrupt turns. This can be done if a circular arc is used which is tangent to the discharge angle at one end and tangent to a line perpendicular to the slot axis at the other end. The radius of the arc would then be determined by the vane height and the discharge angle. This is illustrated in Figure 11.

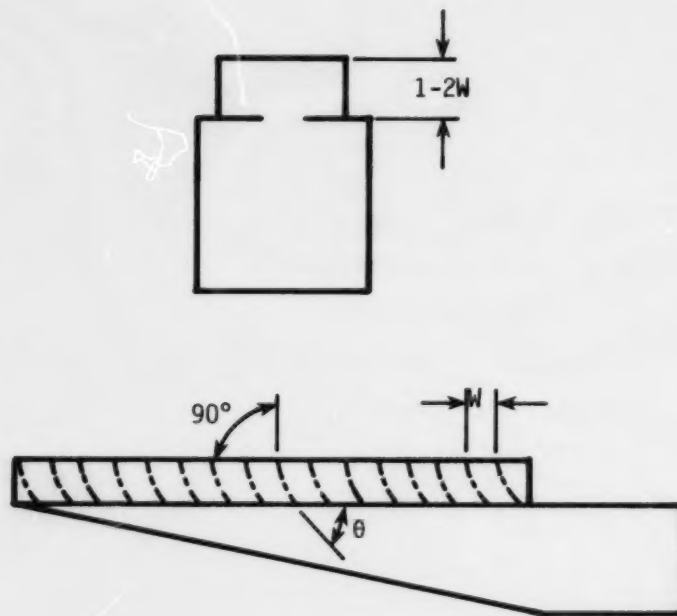


Figure 11. Application of turning vanes to air curtain slot.

According to the Heating, Ventilating, Air Conditioning Guide, vanes should have a depth of one to two times the spacing. If the ratio is less than one, effective control of the discharging air stream cannot be obtained. Little improvement is realized by increasing the ratio beyond two.

Baturin (1972), based on experience of various air curtain installations, recommended the use of flat deflector vanes for air curtains with tapered manifolds. The recommended air outlet slot design is shown in Figure 12. The deflector vanes are boxed in. The vanes are spaced one slot width apart and the vane height should be 2.5 to 3.0 times the slot width.

Air Curtain Flow Field

An air curtain flow field approximates a submerged two-dimensional plane jet. There have been a large number of investigations of turbulent jets. The following is a brief review of the theories and experiments of two dimensional jets with zero background air movement.

A jet of air will mix at its periphery with the surrounding air so that with increasing distance from the nozzle the volume of air flow constantly increases and the velocity decreases. The mixing action is due to simple turbulence rather than an effect of negative static pressure. Air from the surrounding mass moves inward to the periphery of the expanding stream for its full length to replace that entrained into motion by mixing. Continuance of the motion is due solely to the momentum of the air jet.

The flow field produced by a two-dimensional jet exhausting into still surrounding can be broken into three regimes. Where the jet exits from the nozzle, as shown schematically in Figure 13, regions of turbulent mixing are formed at either edge of the plane jet. The width of the turbulent mixing region expands in the downstream direction, so that it encroaches both on the external still air and on the non-turbulent potential core region between the two mixing layers. At the point where the two mixing layers meet, the potential core disappears, and the potential core region (or first regime of

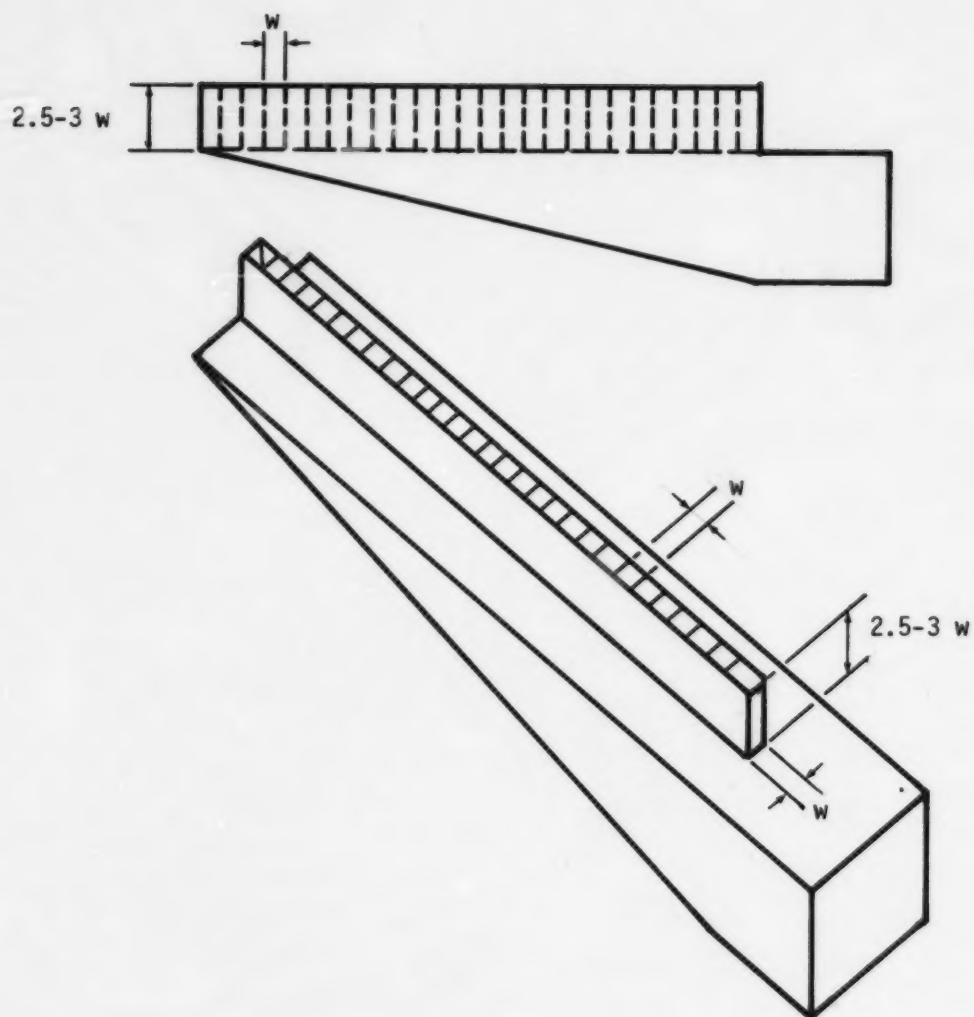


Figure 12. Air curtain outlet design recommended by Baturin.

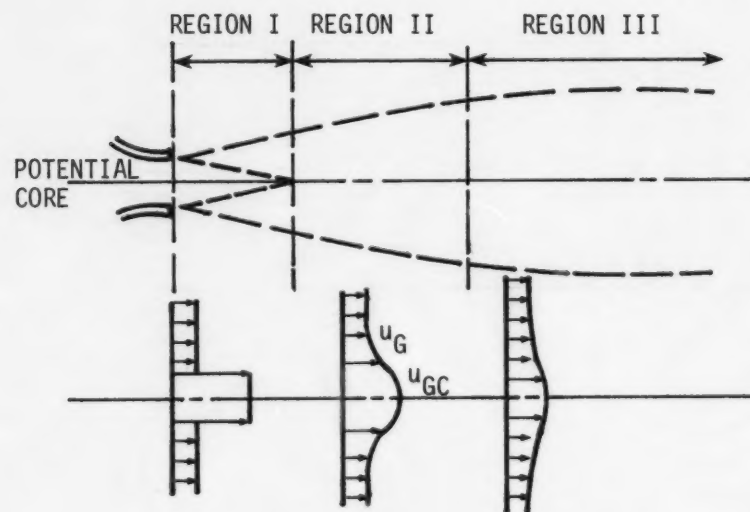


Figure 13. Different regions and velocity profiles for a turbulent jet.

mixing) undergoes a transition to the second regime in which turbulent flow is encountered all the way across the jet. The potential core length is usually very short; therefore, the jet becomes completely turbulent at a short distance from the point of discharge.

Region II starts at about 4 nozzle widths downstream from the nozzle and ends at about 8 nozzle widths downstream from the nozzle. This region is characterized by both the absence of a potential core and the absence of similarity in the mean velocity distribution. This region is followed by region III which is characterized by similar velocity distribution.

The mixing model just described is idealized. An ideal two-dimensional jet has no characteristic length, which means not only that the fluid viscosity and jet velocity completely specify the whole flow, but also that a characteristic Reynolds number for the whole flow cannot be defined. This in turn implies that all two-dimensional jets are dynamically similar. Under this condition, the velocity profile can be found from equations of motion and continuity.

$$\frac{\partial u_G}{\partial t} + u_G \frac{\partial u_G}{\partial x} + v_G \frac{\partial u_G}{\partial y} = \frac{1}{\rho_G} \frac{\partial \tau}{\partial y} \quad (24)$$

$$\frac{\partial u_G}{\partial x} + \frac{\partial v_G}{\partial y} = 0 \quad (25)$$

Where τ = turbulent shear stress, g/cm^2
 u_G = "x" component of gas velocity, cm/s
 v_G = "y" component of gas velocity, cm/s
 ρ_G = gas density, g/cm^3
 x = coordinate parallel to gas stream, cm
 y = coordinate perpendicular to gas stream, cm
 t = time, s

There are several shear stress and eddy viscosity models proposed for use in incompressible flow calculations. The most commonly used models are the Prandtl mixing theory and the Prandtl eddy viscosity model. Other available models are Taylor's vorticity transport theory and Reichardt's inductive hypothesis.

Velocity fields of a free plane jet predicted from Prandtl's theories have been verified by experimental data of Fage and Falkner (1932). However, they found there are no similarities between the velocity and temperature fields. Taylor (1932) removed an inconsistency of Prandtl's mixing length theory and assumed that the turbulent shearing stress in a stream must be determined by the lateral transfer of vorticity and not, as Prandtl assumed, by momentum transfer. Taylor's theory has been confirmed by the experiments of Fage and Falkner (1932). Even though Taylor's theory is built on more justifiable assumptions, it gives a velocity profile no more accurately than the Prandtl theory did.

Since we are interested in the velocity fields of a free plane jet, only Prandtl's theories will be presented in the following section. Of the two Prandtl's theories, the eddy viscosity theory gives velocity profiles which are described by relatively simple analytic formulas. In the mixing length theory, velocity profiles can only be determined numerically. The Prandtl eddy viscosity theory gives better agreement with experimental data in the region close to the jet centerline, while the Prandtl mixing length theory gives better agreement with data in the region close to the jet boundary.

Prandtl Mixing Length Theory

The fundamental idea of mixing length theory is that turbulent mixing can be thought of as taking place between discrete "lumps" of turbulent fluid. These "lumps" are envisioned as leaving a region of the flow with a velocity characteristic of that region, and then moving a certain lateral distance, called the mixing length, as a discrete particle before suddenly mixing with other fluid of different velocities. The actual mathematical development of this idea is gone into in detail by Alexander et al., (1953) who shows that

necessary assumptions in the development are that the turbulent intensity and mixing length are isotropic. Since a turbulent shear flow is anisotropic, this is a rather crude assumption; it can, however, be rationalized by assuming that the differences between the fluctuating coefficients are of second-order effect compared to the degree of correlation of the shear stress.

In its final form, which is strictly valid only for two-dimensional or axisymmetric flow, the turbulent shear stress is:

$$\tau = \rho_G \ell^2 \left| \frac{\partial u_G}{\partial y} \right| \frac{\partial u_G}{\partial y} \quad (26)$$

Where τ = shear stress, g/cm²
 ρ_G = gas density, g/cm³
 u_G = longitudinal gas velocity, cm/s
 y = coordinate perpendicular to jet axis, cm
 ℓ = mixing length, cm

Tollmein (1926) applied Prandtl's mixing length theory to the boundary layer of an infinite plane-parallel jet and obtained:

$$\frac{u_G}{u_{Gc}} = F'(\phi) = \frac{dF(\phi)}{d\phi} \quad (27)$$

$$\phi = \frac{y}{ax} = 1.81 - \frac{2}{3} \left[\ln(Z^{0.5} + 1) - \ln(Z - (Z^{0.5} + 1))^{0.5} \right. \\ \left. + 1.732 \tan^{-1} \left(\frac{2Z^{0.5} - 1}{1.732} \right) \right] \quad (28)$$

$$Z = z' \frac{dz}{d\phi} \quad (29)$$

$$z = \ln F(\phi) \quad (30)$$

Where u_{Gc} = centerline velocity at "x" cm downstream from jet exit, cm/s
 u_G = air velocity at a distance "y" from the centerline, cm/s
 ϕ = coordinate defined as y/ax , dimensionless
 a = experimental constant, dimensionless

The experimental constant "a" can be obtained by comparing the centerline velocity decay with experimental data. It is 0.11 for Förlthmann's data (1936), 0.12 for Proskura's data (1938), and 0.09 for Turkus' data (1941).

Equations (27) through (30) can only be solved numerically. Table 1 is a tabulation of $F'(\phi)$ values at different " ϕ " which could be used for constructing the velocity profile.

The centerline velocity of a plane jet is itself a function of distance from the pole or nozzle exit of the jet. The centerline velocity can be derived using the conservation of momentum in a free plane jet. The final formula for the centerline velocity is:

$$\frac{u_{Gc}}{u_{Gj}} = \frac{1.2}{\left(\frac{2ax}{w}\right)^{0.5}} = (\text{constant}) \left(\frac{x}{w}\right)^{-0.5} \quad (31)$$

Where u_{Gc} = centerline air velocity, cm/s
 u_{Gj} = nozzle exit jet velocity, cm/s
 x = distance downstream from jet pole or jet nozzle exit, cm
 w = slot width, cm

Prandtl Eddy Viscosity Model

The Prandtl eddy viscosity model or the constant exchange coefficient model is based on the expression for the shear stress in a laminar flow.

TABLE 1. "F' (ϕ)" VALUES AT DIFFERENT " ϕ "

ϕ	$\frac{u_G}{u_{Gc}} = F'(\phi)$
0	1.000
0.1	0.979
0.2	0.940
0.3	0.897
0.4	0.842
0.5	0.782
0.6	0.721
0.7	0.660
0.8	0.604
0.9	0.538
1.0	0.474
1.1	0.411
1.2	0.357
1.3	0.300
1.4	0.249
1.5	0.200
1.6	0.165
1.7	0.125
1.8	0.095
1.9	0.067
2.0	0.046
2.1	0.030
2.2	0.020
2.3	0.009
2.4	0

$$\tau_L = \rho_G \nu_G \frac{\partial u_G}{\partial y} \quad (32)$$

Where τ_L = laminar flow shear stress, g/cm^2
 ρ_G = air density, g/cm^3
 ν_G = kinematic viscosity of gas, cm^2/s
 u_G = longitudinal component of gas velocity, cm/s
 y = coordinate perpendicular to the jet axis, cm

It is postulated that the turbulent shear stress can be written in terms of an effective exchange coefficient or eddy viscosity so that in a turbulent flow

$$\tau = \rho_G \epsilon \frac{\partial u_G}{\partial y} \quad (33)$$

Where τ = turbulent shear stress, g/cm^2
 ϵ = eddy viscosity, cm^2/s

The most commonly used eddy viscosity formulation is the model proposed by Prandtl (Abramovich, 1963; Schlichting, 1968) which may be written:

$$\epsilon = kb |u_{Gc} - u_{Gb}| \quad (34)$$

Where ϵ = eddy viscosity, cm^2/s
 k = constant, dimensionless
 b = width of mixing region, cm
 u_{Gc} = centerline velocity, cm/s
 u_{Gb} = velocity at the edges of the mixing zone, cm/s

Applying this friction law, Görtler (1942) worked out a new theory for the two-dimensional jet. His solution for the axial component of the velocity is

$$u_G = \frac{0.3^{0.5}}{2} \left(\frac{J \sigma}{\rho_G x} \right)^{0.5} \left(1 - \tanh^2 \left(\sigma \frac{y}{x} \right) \right) \quad (35)$$

Where u_G = axial component of the velocity, cm/s
 J = momentum per unit length of slot, g/s²
 ρ_G = air density, g/cm³
 x = distance downstream from slot, cm
 σ = experimental constant = 7.67, dimensionless

Application of the Flow Field Theories to Air Curtains

Two important parameters of the SCAT air curtain, the air entrainment ratio and the jet boundary expansion angle, can be derived from the plane jet theories presented earlier.

Since momentum is conserved in a free plane jet, the momentum, "J", in equation (35) can be substituted by the value at the jet nozzle exit, i.e.

$$J = \rho_G u_{Gj}^2 w \quad (36)$$

Where J = momentum per unit slot length, g/s²
 ρ_G = air density, g/cm³
 u_{Gj} = slot exit velocity, cm/s
 w = slot width, cm

Substituting equation (36) into (35), the following equation is obtained:

$$\frac{u_G}{u_{Gj}} = 2.4 \left(\frac{x}{w} \right)^{-0.5} \left(1 - \tanh^2 \frac{7.67y}{x} \right) \quad (37)$$

Where u_G = axial component of velocity, cm/s
 x = distance downstream of slot, cm
 y = distance from axis of the jet, cm

The centerline velocity of the jet stream can be calculated from equation (37) by letting $y = 0$, i.e.

$$\frac{u_{Gc}}{u_{Gj}} = 2.4 \left(\frac{x}{w}\right)^{-0.5} \quad (38)$$

Where u_{Gc} = centerline velocity, cm/s

The constant in equation (38) is slightly different if Prandtl's mixing length theory (equation 31) is used. It will be 2.45 for $a = 0.12$, 2.56 for $a = 0.11$, and 2.83 for $a = 0.09$. McElroy (1943) suggested a constant of 2.45 be used for estimating the centerline velocity decay.

The jet expansion angle and the air entrainment can also be evaluated if the jet boundary is known. In Görtler's theory, the boundary layer is found to asymptotic, i.e., extending from $y = -\infty$ to $y = +\infty$. In Tollmein's theory, the boundary layer is of finite thickness. The two theories would be equivalent if one assumed that the boundary is the location at which the axial velocity is a small fraction of the centerline velocity, e.g., $0.01 u_{Gc}$. Abramovich (1963) gave the following approximate expression for estimating the half width of a plane jet:

$$b = 0.22x \quad (39)$$

Where b = half width of the plane jet, cm
 x = distance downstream from the slot, cm

Consequently, the inclusive jet expansion angle is 25° .

The air entrainment ratio can be calculated by first estimating the average axial velocity followed by performing a momentum balance. The average axial velocity is

$$u_{Gx} = \frac{1}{b} \int_0^b u_G dy \quad (40)$$

Where u_{Gx} = average axial velocity at "x" meter downstream from the nozzle, cm/s

b = half width of the jet stream, cm

u_G = axial component of velocity, cm/s

y = distance from the jet axis, cm

Substituting equations (35) and (39) into equation (40) and performing the integration, the following equation is obtained:

$$\frac{u_{Gx}}{u_{Gj}} = 1.32 \left(\frac{w}{x}\right)^{0.5}, \text{ or} \quad (41)$$

$$\frac{u_{Gc}}{u_{Gx}} = 1.81 \quad (42)$$

Where u_{Gx} = average axial velocity, cm/s

u_{Gj} = slot exit velocity, cm/s

u_{Gc} = centerline velocity, cm/s

w = slot width, cm

x = distance downstream from the slot, cm

Since momentum is conserved in a free jet, a momentum balance on the jet yields:

$$u_{Gj}^2 A_j = u_{Gx}^2 A_x \quad (43)$$

Where u_{Gj} = slot exit velocity, dm/s
 u_{Gx} = average axial velocity, cm/s
 A_j = cross-sectional area of the slot, cm²
 A_x = cross-sectional area of the air stream at "x" cm from the slot, cm²

Since volumetric flow rate is equal to the product of $u_G A$, one may write

$$Q_{Gj} u_{Gj} = Q_{Gx} u_{Gx} \quad (44)$$

$$q = \frac{u_{Gx}}{Q_{Gj}} = \frac{u_{Gj}}{u_{Gx}} = 0.758 \left(\frac{x}{w}\right)^{0.5} \quad (45)$$

Where q = air entrainment ratio, cm³/cm³
 Q_{Gj} = volumetric flow rate at the slot exit, cm³/s
 Q_{Gx} = volumetric flow rate of the air stream at "x" cm from the slot, cm³/s
 w = slot width, cm
 x = distance downstream from the slot, cm

McElroy (1943) gave a different equation for estimating the average axial velocity at some downstream locations. His equation is

$$\frac{u_{Gc}}{u_{Gx}} = 2.5 \quad (46)$$

Using McElroy's equation, the air entrainment ratio is

$$q = \frac{Q_{Gx}}{Q_{Gj}} = 1.02 \left(\frac{x}{w}\right)^{0.5} \quad (47)$$

Abramovich (1963) took a different approach in calculating the volume of air per second which flows past an arbitrary section of the main region (region III) of a submerged plane jet. He used the following equation:

$$Q_{Gx} = \int_0^F u_G dF \quad (48)$$

Where Q_{Gx} = volumetric flow rate of the air stream at "x" cm from the slot, cm^3/s

u_G = axial component of velocity, cm/s

F = function which is used to define the stream function

In terms of flow rate at the slot exit, equation (48) becomes

$$q = \frac{Q_{Gx}}{Q_{Gj}} = \frac{1}{u_{Gj} F_j} \int_0^F u_G dF = \frac{u_{Gc}}{u_{Gj}} \frac{F}{F_j} \int_0^1 \frac{u_G}{u_{Gc}} \frac{dF}{F} \quad (49)$$

Abramovich (1963) solved equation (49) and obtained

$$q = 0.375 \left(\frac{2x}{w}\right)^{0.5} = 0.53 \left(\frac{x}{w}\right)^{0.5} \quad (50)$$

Where q = air entrainment ratio, cm^3/cm^3

w = slot width, cm

x = distance downstream from the slot exit, cm

EXPERIMENTS

Distribution Manifold

Experiments were performed to study air curtain design and to verify the air curtain flow field. As mentioned earlier, the air curtain in the SCAT system should have a small expansion angle, small air entrainment ratio, and a uniform velocity distribution. As has been presented earlier, the distribution manifold should have a constant static pressure from end to end to obtain uniform velocity distribution. To obtain constant static pressure, the manifold should have either a very large cross-sectional area compared to the total side outlet area of a decreasing cross-sectional area to keep the velocity head in the manifold constant.

Early attempts to simplify the construction of an air curtain manifold by drilling 2.54 cm diameter holes along a 30.5 cm diameter duct did not yield uniform flow. The discharge direction was also not perpendicular to the axis of the manifold. These findings are expected since the duct cross-sectional area was not much larger than the total hole area.

A better distribution manifold shown in Figure 14 was built. The manifold had a 35.6 cm x 30.5 cm (14 inches x 12 inches) rectangular cross section. An adjustable plate was placed in the duct to decrease the flow area along the duct. The air jet nozzle was a continuous slot 1.2 m (7 ft) in length. The slot was formed by two parallel plates which protruded 22.9 cm (9 inches) from one side of the duct. The distance between the plates, which is the slot width, could be adjusted.

The slot was divided by thin deflector vanes at 5.1 cm (2 inches) apart. Instead of curved turning vanes as suggested by Koestel and Young (1951), thin flat plates were used. Since the air curtain manifold was intended for testing at a variety of operating conditions, the use of thin flat plates eliminated the need to change the turning vanes for each operating condition as would

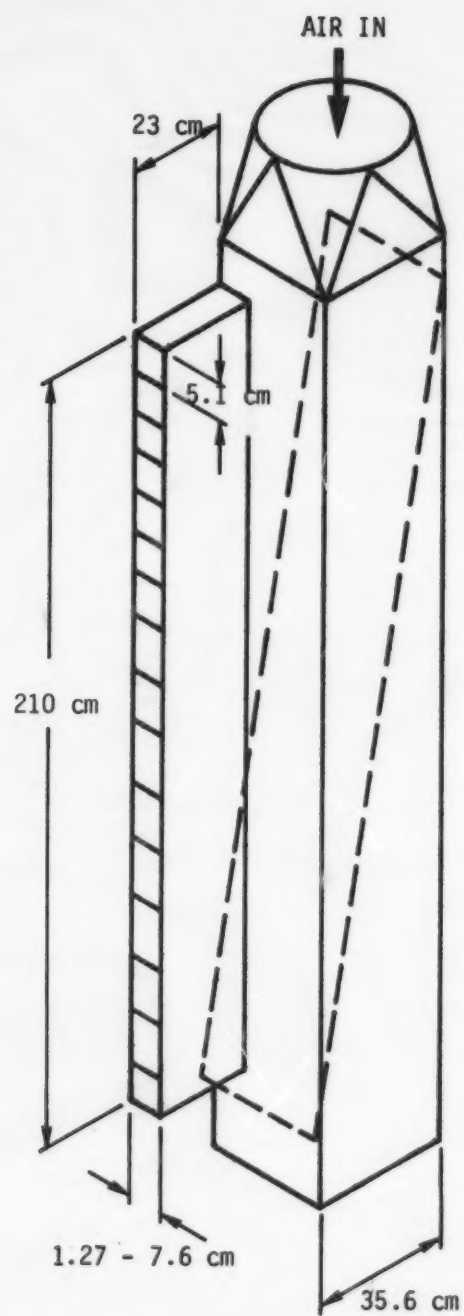


Figure 14. Air curtain distribution duct.

have been necessary if curved turning vanes were used. The construction was also simpler with thin flat plates.

Experiments

The air discharge angle was estimated by attaching small flags to the slot exit. It was judged to be perpendicular to the longitudinal axis of the manifold for a slot width up to 7.6 cm (3 inches).

The air discharge velocities along the slot were measured with a calibrated constant temperature thermal anemometer. Figure 15 shows the data for a slot width of 1.3 cm (0.5 inches). The manifold was oriented vertically. As can be seen, the velocities are fairly uniform except at the bottom 0.3 m (1 ft) of the slot where they are about 20 percent lower than the rest.

The bottom 0.3 m (1 ft) of the slot was covered, and the resulting velocities had a slightly more uniform distribution as shown in Figure 16.

The velocity distribution was measured for three vertical levels at three downstream locations. Figure 17 shows the data for a slot width of 1.3 cm (0.5 inch) and an exit velocity of 23 m/s. Since the velocity profiles for the three vertical locations at the same downstream location are virtually one on top of another, the vertical velocity distribution was assumed to be uniform and subsequent velocity distribution measurements were done at the mid-level of the manifold.

Velocity distributions were measured at several downstream locations for slot widths of 1.3 cm, 2.5 cm, 3.8 cm, and 5.1 cm and slot exit velocities up to 30 m/s. Figure 18 shows a typical velocity distribution. The jet expansion angle was calculated by estimating the jet width from the velocity distribution plots and according to the following equation

$$\theta = \tan^{-1} \frac{b}{x} \quad (51)$$

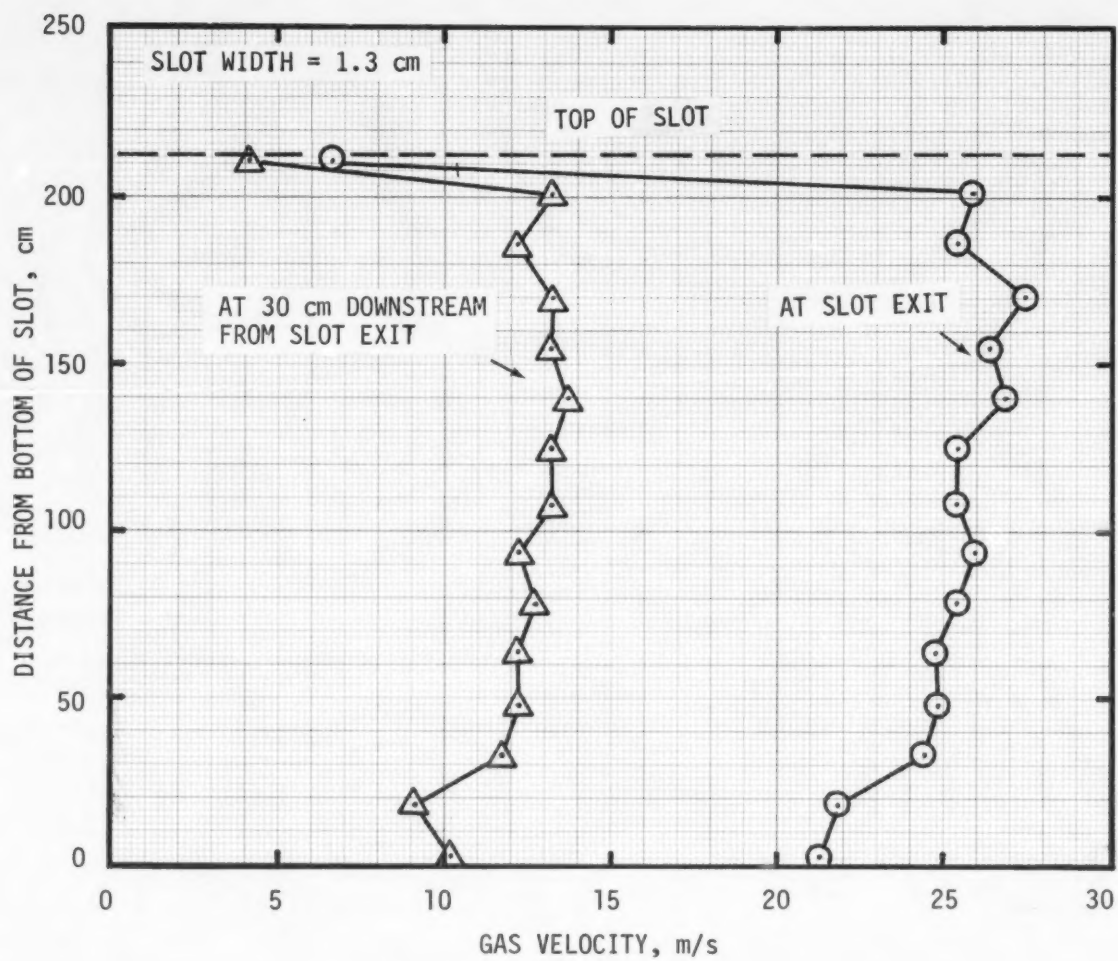


Figure 15. Velocity distribution along the air curtain.

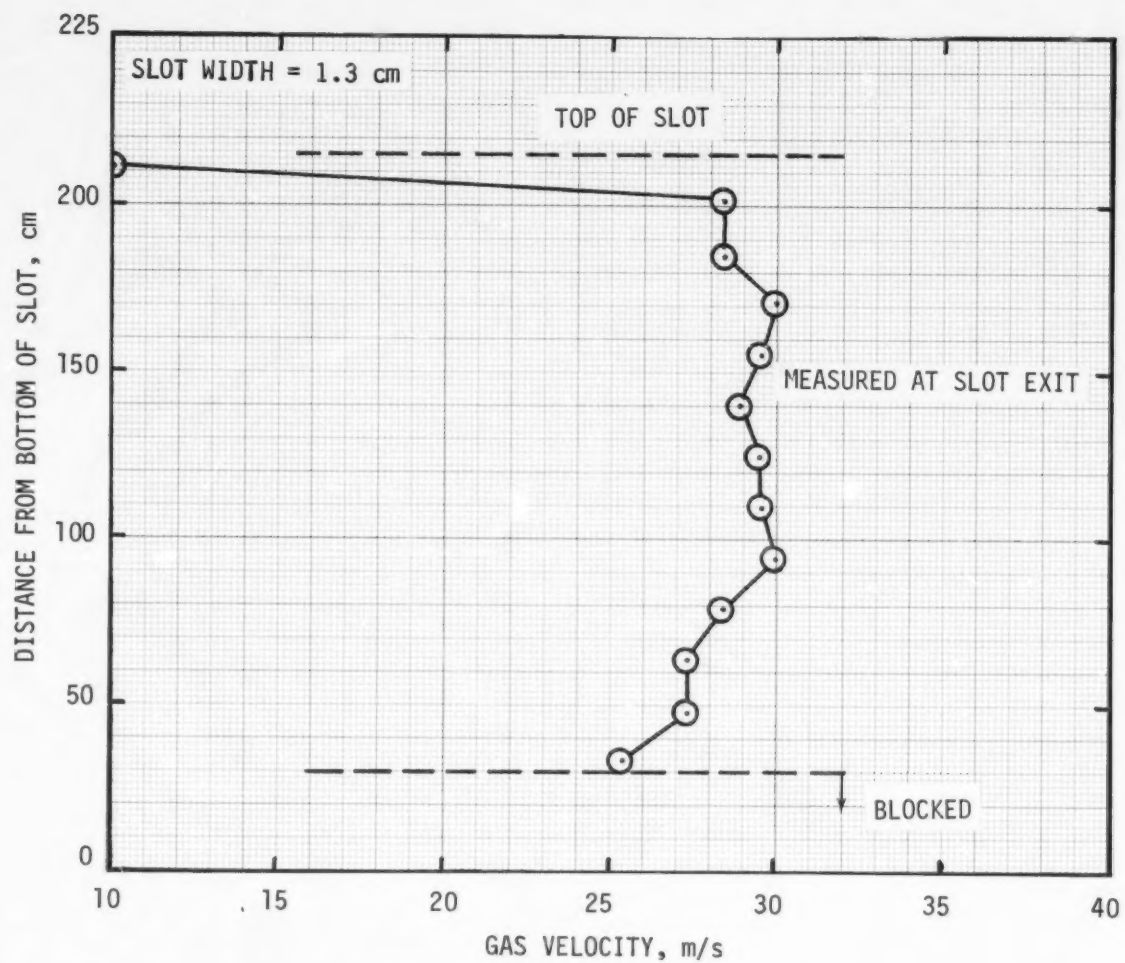


Figure 16. Velocity distribution along the air curtain.

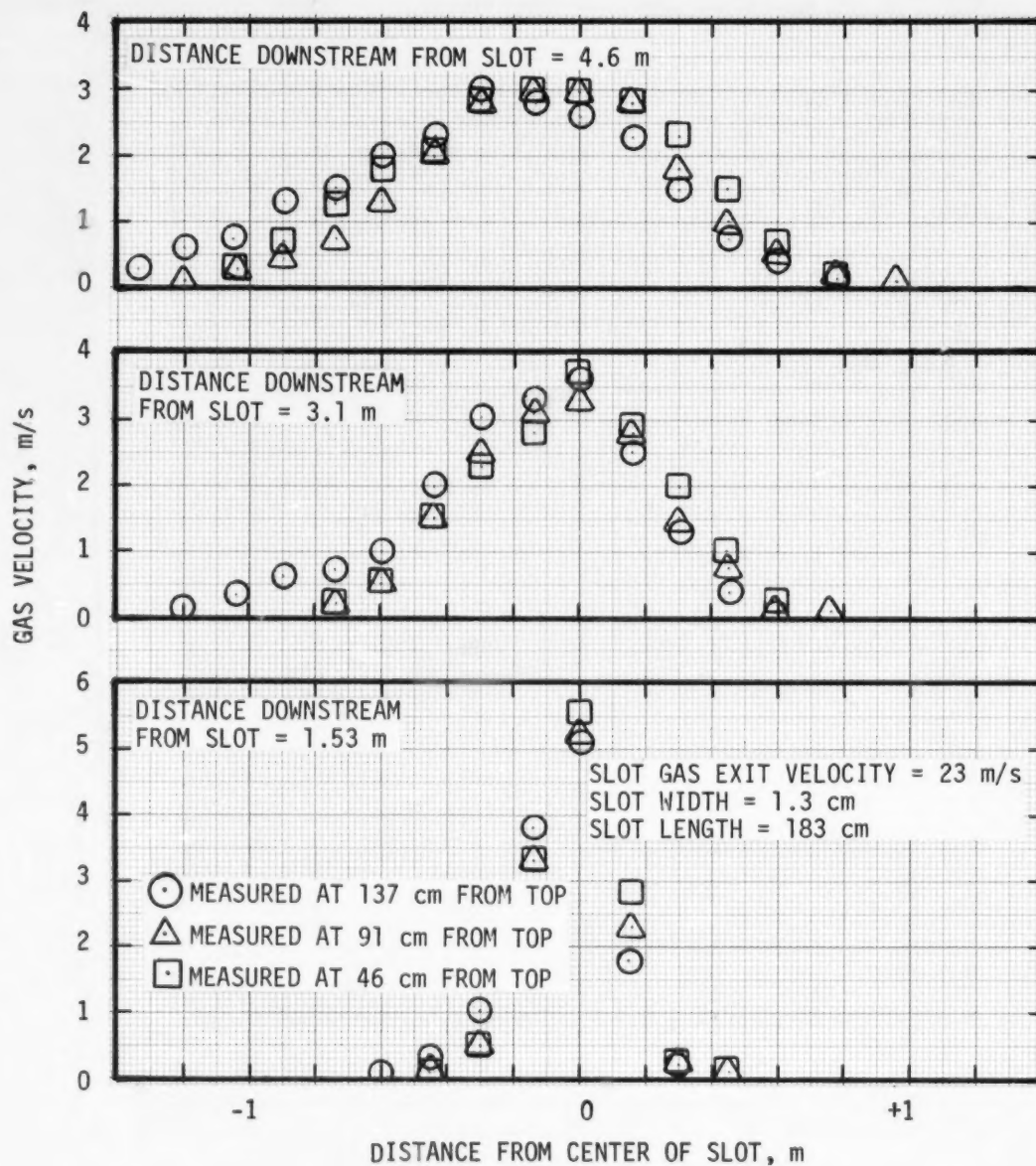


Figure 17. Velocity profile of an air curtain.

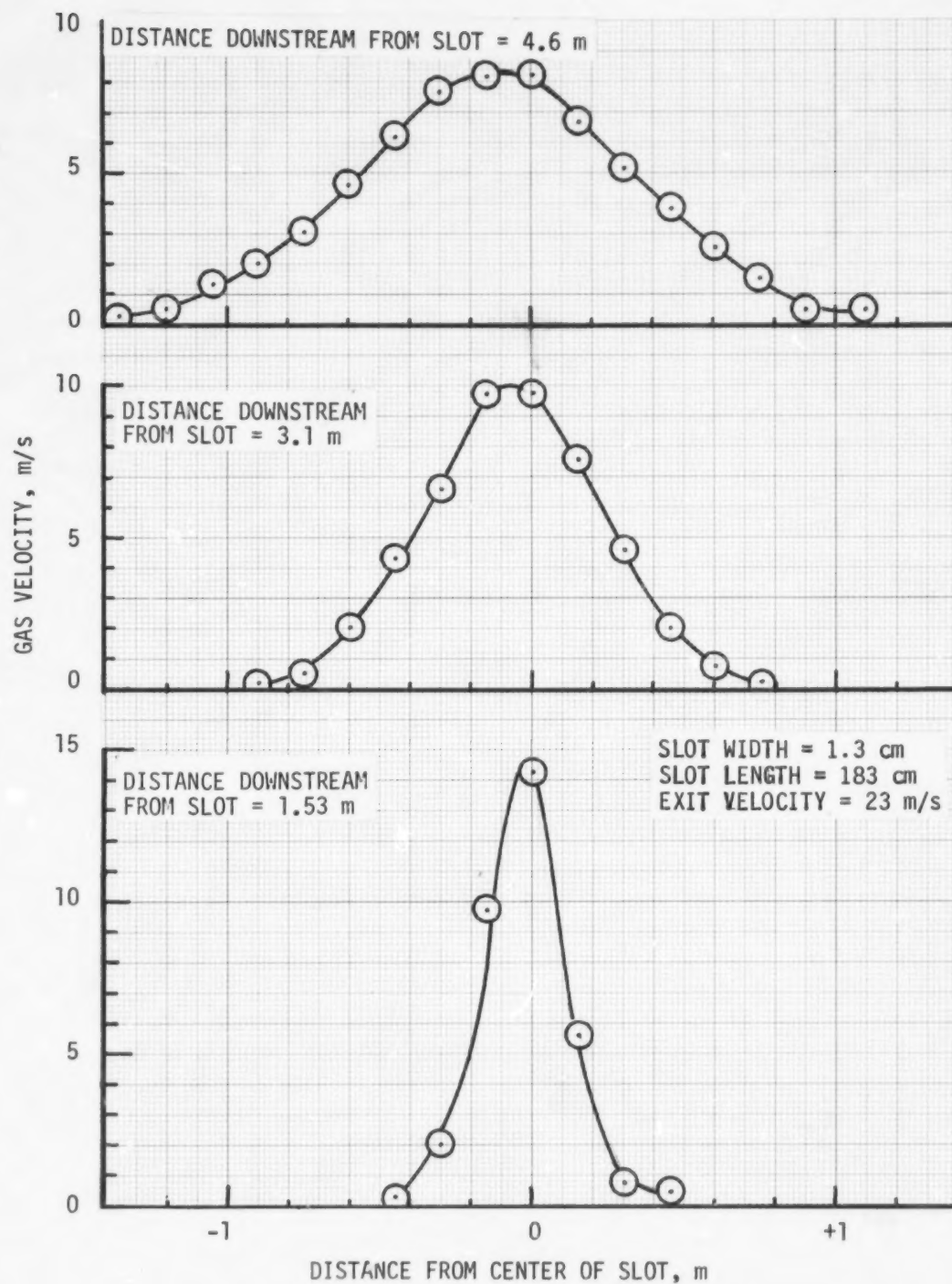


Figure 18. Velocity profile of an air curtain.

Where θ = jet expansion angle, degree
 b = half width of jet, cm
 x = distance downstream from slot, cm

The air entrainment ratio was calculated by graphically integrating the velocity distribution plot and then dividing it by the volume flow per unit length of slot at the slot exit.

Figure 19 shows the measured and the predicted centerline axial velocity decay. The agreement between measurements and predictions is good.

Figure 20 shows the measured and the predicted air entrainment ratio. The measured air entrainment ratio is much less than that predicted from McElroy's equation. It is close to those predicted from Abramovich's equation and equation (45). The measured air entrainment ratio can be described by:

$$q = \frac{Q_{Gx}}{Q_{Gj}} = 0.66 \left(\frac{x}{w}\right)^{0.5} \quad (52)$$

which is equal to the average of that predicted from equation (45) and Abramovich's equation.

The measured jet expansion angle varied between 20° and 28° , depending on air exit velocity. A larger expansion angle was obtained with a higher air exit velocity from the slot. The average jet expansion agrees with the 25° calculated by Abramovich (1963) for pure momentum jet. However, it is smaller than the $32^\circ - 34^\circ$ reported by Tuve and Priester (1944) and $30^\circ - 40^\circ$ suggested by Hemeon (1963) for air curtain design.

These experiments showed that the air curtain distribution manifold performed satisfactorily. A pilot scale SCAT system was designed and built using this design for the air curtains. The pilot scale SCAT was used to study the effects of hot buoyant plume and crosswind on air curtain performance. The experiments are presented in a later section.

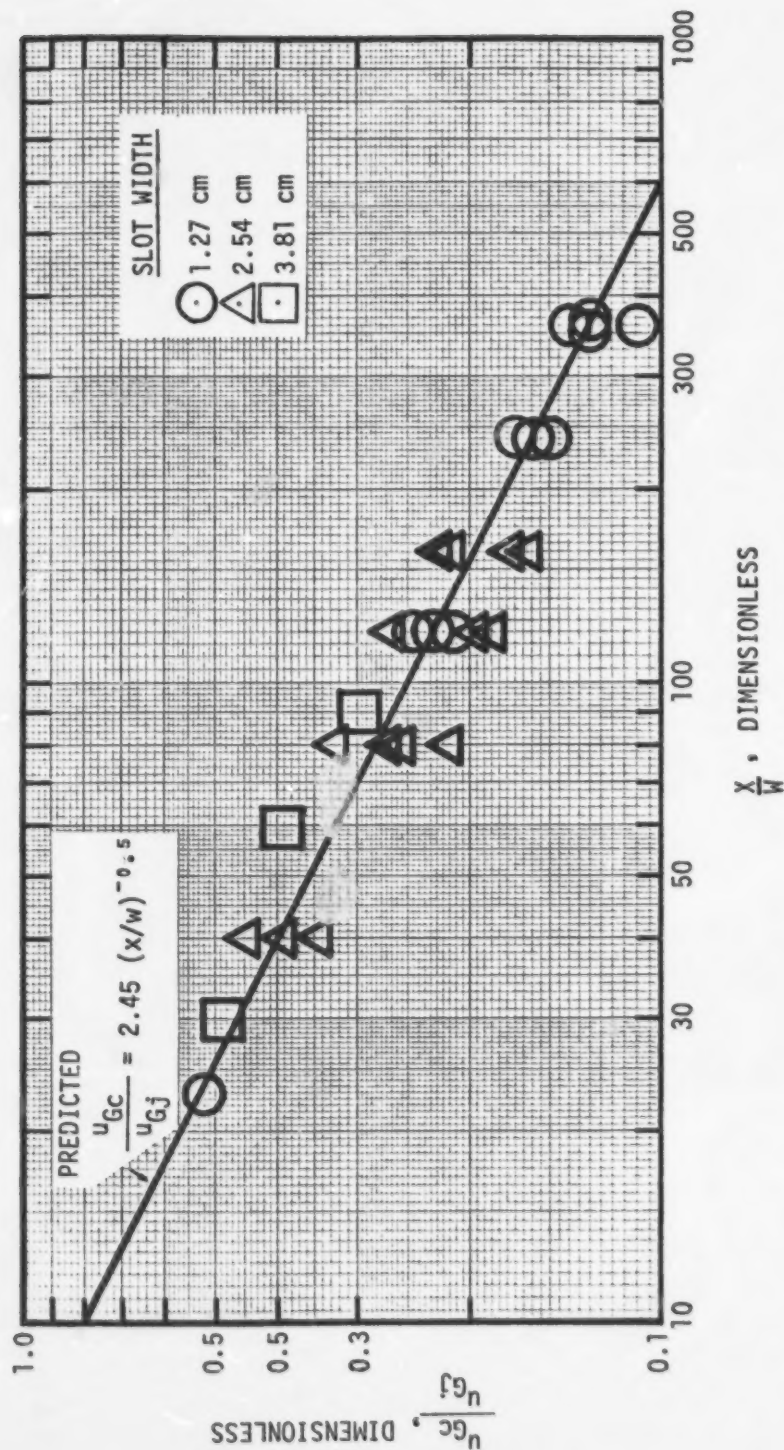


Figure 19. Measured and predicted centerline axial velocity decays.

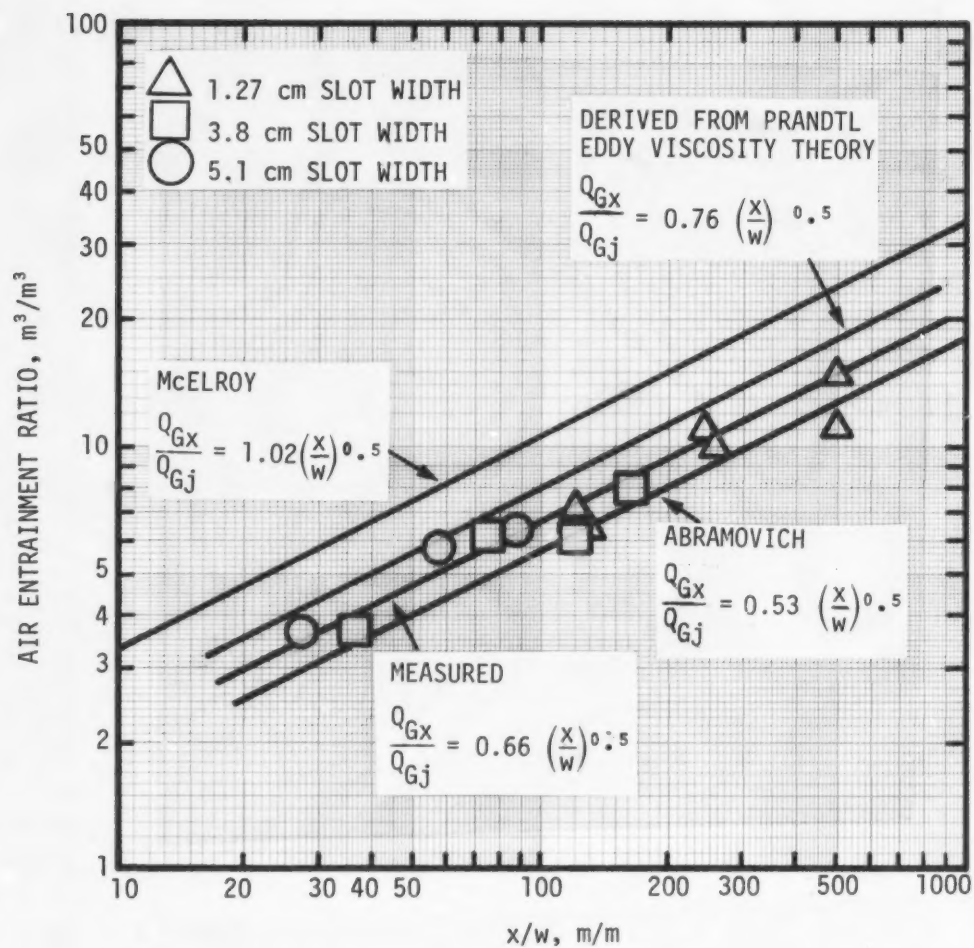


Figure 20. Measured and predicted air entrainment ratios.

SECTION 5

SPRAY SCRUBBER STUDY

The effectiveness of the SCAT system in controlling fugitive particles depends on the particle containment and conveying efficiency of the air curtain as well as the particle removal efficiency of the spray scrubber. Containment and removal efficiencies were studied individually. This section presents the results of the particle removal experiments.

LITERATURE SEARCH

Spray Scrubber Mathematical Model

Literature was searched for spray scrubber design equations. Three models were found. They were reported by:

1. Calvert, et al. (1975)
2. Cheng (1973), and
3. Crawford (1976).

Model of Calvert, et al.

Calvert, et al. (1975) developed the spray scrubber model by considering the volume of gas swept clean by drops. Their equation for particle penetration is:

$$\begin{aligned} P_{t_d} &= \exp \left(- \left(v_s \frac{Q_L}{Q_G} \right) \right) \\ &= \exp \left(- \frac{3}{2} \frac{Q_L}{Q_G} \frac{R_d}{d_d} \bar{n} \right) \end{aligned} \quad (53)$$

Where Pt_d = particle penetration for diameter " d_{pa} ", fraction
 V_s = gas volume swept clean per cm^3 of drops, cm^3/cm^3
 Q_L = liquid flow rate, cm^3/s
 Q_G = gas flow rate, cm^3/s
 R_d = drop range or distance traveled by drop relative to the gas, cm
 $\bar{n}$ = average particle collection efficiency of drop over the drop range, fraction
 d_d = drop diameter, cm

The following assumptions were used in their derivation of the model:

1. The particle concentration is uniform as the aerosol enters a spray stage and that it is completely mixed (i.e., uniform concentration) before entering the next stage.
2. Drops are of uniform diameter.
3. No wall losses of drops.
4. Particle collection is by inertial impaction on the front of the drop only.
5. Spray coverage is not 100% at the entrance of a spray stage. The coverage will be 100% when the spray cones touch each other. Drops are uniformly distributed when 100% coverage has been attained.

The average single drop collection efficiency, $\bar{n}$, is computed from

$$\bar{n} = \frac{1}{R_d} \int_0^{R_d} n f_A dx \quad (54)$$

Where η = instantaneous single drop collection efficiency, fraction
 $\bar{\eta}$ = average single drop collection efficiency, fraction
 f_A = fraction of gas flow cross section covered by sprays, fraction
 R_d = drop range or distance travelled by drop relative to the gas, cm
 x = coordinate, cm

By combining equations (53) and (54), the following equation is obtained

$$Pt_d = \exp \left[- \frac{3}{2} \frac{Q_L}{Q_G} \frac{1}{d_d} \int_0^{R_d} \eta f_A dx \right] \quad (55)$$

The model of Calvert et al. has some limitations. They assumed that drops are uniformly distributed. Depending on nozzle arrangement, this may be true for full cone spray nozzles. Pigford and Pyle (1951) investigated the mass distribution of drops in sprays produced by several commercial full cone nozzles. They found that the mass of water was uniformly distributed throughout the spray except at the edge. With hollow cone sprays, the drops are localized at the edge of the spray cone. The assumption of uniform drop distribution is no longer true.

Since drops are not travelling parallel to the gas flow, there must be some drop wall losses. The effective liquid/gas ratio will be lower than the actual ratio. Equation (55) will under estimate the particle penetration. One can bring " Q_L/Q_G " into the integration sign of equation (55) to account for changing liquid/gas ratio, i.e.

$$Pt_d = \exp \left[- \frac{3}{2} \frac{1}{d_d} \int_0^{R_d} \eta f_A \frac{Q_L}{Q_G} dx \right] \quad (56)$$

Effective liquid/gas ratio can be determined from spray angle, nozzle location, and drop trajectory. One can assume that a drop which collides with the wall will stick on the wall and no longer be effective for removal of particles.

The single drop collection efficiency, " η ", can be calculated from the particle trajectory. Many researchers have presented results of such calculations and a brief review of the theory and calculations is presented later in this chapter.

Cheng Model

Cheng (1973) gave the following equation for calculating collection efficiency for confined coaxial sprays:

$$PT_d = \exp \left[- \frac{3}{2} \bar{\eta} \frac{L}{d_d} \frac{\lambda Q_L}{Q_G} \right] \quad (57)$$

where Pt_d = penetration for particle diameter " d_p ", fraction

L = mean effective length of the spray, cm

Q_L = liquid flow rate, cm^3/s

Q_G = gas flow rate, cm^3/s

$\bar{\eta}$ = average single drop collection efficiency, fraction

λ = effective water consumption, fraction

The effective water consumption, " λ " was derived for a full cone nozzle located at the center of a duct:

$$\lambda = \frac{3 \left(\frac{R_d}{R} \right) - 2 \cot \alpha}{2 \left(\frac{R_d}{R} \right)^3 - (1 - \cos \alpha)} \quad (58)$$

Where α = spray angle, degree

R = duct radius, cm

R_d = projection distance of the spray or the distance travelled by the drop, cm

The mean effective length, "L", is calculated from the following equation:

$$L = R_d - \frac{2R}{3} \cot \alpha \quad (59)$$

Where α = spray angle, degree

R = duct radius, cm

R_d = drop range in distance travelled by drop relative to gas, cm

Assumptions used by Cheng in deriving his equation are the same as Calvert, et al. except for wall losses. Wall losses were taken into account in the " λ " factor in Cheng's model. Cheng derived the effective water consumptions, " λ ", for a full cone spray nozzle located at the center of a circular duct. It cannot be applied to hollow cone nozzles and other nozzle locations. Therefore, the applicability of his equation is limited.

Model of Crawford

Crawford (1976) gave the following equation for calculating spray scrubber collection efficiency.

$$\Delta N_p = \frac{\pi d_d^2}{4} n_p N_d \int_0^{R_d} n \, dx \quad (60)$$

Where ΔN_p = rate at which particles are collected by N_d drops, s^{-1}
 N_d = rate at which drops are injected into the air stream, s^{-1}
 d_d = drop diameter, cm
 n_p = inlet particle number concentration, cm^{-3}
 n = single drop collection efficiency, fraction
 R_d = drop range, cm

Assumptions used in deriving this equation are:

1. Drops are uniformly distributed and of uniform diameter.
2. No drop wall losses.
3. Drops travel parallel to gas stream.
4. No particle mixing.

The integration in equation (60) is performed backwards, i.e. from downstream location which has zero drop velocity to the nozzle.

Since $n_p = N_p/Q_G$, $N_d = 6 Q_L/\pi d_d^3$, and $E = \Delta N_p/N_p$, equation (60) can also be put into the following form:

$$E = \frac{3}{2} \frac{Q_L}{Q_G} \frac{1}{d_d} \int_0^{R_d} n \, dx \quad (61)$$

Where E = collection efficiency for particle diameter " d_{pa} ", fraction
 Q_L = liquid flow rate, cm^3/s
 Q_G = gas flow rate, cm^3/s
 d_d = drop diameter, cm
 n = single drop collection efficiency, fraction
 R_d = distance travelled by drop relative to gas, cm
 x = coordinate parallel to gas flow, cm

Single Drop Collection Efficiency

Single drop collection efficiency is defined as the ratio of the number of particles collected to the number of particles initially contained in the volume swept through by the drop. To the extent that one can rationally predict the behavior of the particles as they flow around and into the drop, one can say that a particle of given diameter and density will strike the drop if it lies initially within a certain distance " y_0 " of the axis of motion of the drop. If the particle lies farther away from the axis than " y_0 ", it will pass by the drop and not be collected. The above reasoning does not apply to diffusional collection mechanism.

If the particles are uniformly distributed throughout the approaching gas stream, then the collection efficiency equals the ratio of area swept clean to the cross-sectional area of the drop, i.e.,

$$\eta = \frac{\pi y_0^2 \sigma}{\pi d_d^2 / 4} = \frac{4 \sigma y_0^2}{d_d^2} \quad (62)$$

Where η = single drop collection efficiency, fraction

σ = attachment coefficient, fraction

d_d = drop diameter, cm

y_0 = distance of the limiting streamline from the axis of drop motion, cm

If it is assumed that all particles stick to the drop upon striking it, $\sigma = 1$.

The distance of the limiting particle trajectory from the axis of drop motion, " y_0 ", can be predicted from Newton's law of motion if the velocity distribution of the gas flowing by the drop and forces acting on the particle are specified. When considering the viscous and electrostatic forces only, the equation of motion for a spherical particle of diameter, d_p , density, ρ_p ,

and for particle Reynolds number less than one is given by a momentum balance as:

$$\frac{1}{6} \pi d_p^3 \rho_p \frac{du_p}{dt} = - \frac{3\pi\mu_G d_p}{C'} (u_p - u_G) + F_e \quad (63)$$

Where d_p = particle diameter, cm
 ρ_p = particle density, g/cm³
 u_G = gas velocity with respect to the drop, cm/s
 u_p = particle velocity with respect to collector, cm/s
 C' = Cunningham slip correction factor, dimensionless
 μ_G = gas viscosity, g/cm-s
 F_e = electrostatic force, g-cm/s²
 t = time, s

Case 1. Neutral Drop and Neutral Particle

When both the particle and drop are neutral, there is no electrostatic force, i.e., $F_e = 0$. Equation (63) in dimensionless form becomes:

$$K_p \frac{du_p^*}{dt^*} = (u_G^* - u_p^*) \quad (64)$$

Where $K_p = \frac{C' \rho_p d_p^2 u_0}{9 \mu_G d_d} = \text{inertial impaction parameter, dimensionless}$

$u_G^* = \frac{u_G}{u_0} = \text{dimensionless gas velocity with respect to the drop, dimensionless}$

$u_p^* = \frac{u_p}{u_0} = \text{dimensionless particle velocity with respect to the drop, dimensionless}$

$$t^* = \frac{2 u_0 t}{d_d} = \text{dimensionless time, dimensionless}$$

u_0 = undisturbed upstream velocity, cm/s

d_d = drop diameter, cm

d_p = particle diameter, cm

ρ_p = particle density, g/cm³

μ_G = gas viscosity, g/cm-s

The solutions to equation (64) depend upon the assumed velocity field around the drop. Langmuir and Blodgett (1946) solved equation (64) for potential flow. Herne (1960) solved equation (64) for both the potential and the viscous flow. Their results are plotted in Figure 21.

The calculations of Herne (1960) for potential flow have been fitted by Knettig and Beeckmans (1974) to:

$$\eta = 0.00376 - 0.232 K_p + 2.42 K_p^2 - 2.025 K_p^3 \quad (65)$$

for $0.0832 \leq K_p \leq 0.6$, and

$$\eta = \frac{4 K_p^2}{(2 K_p + 1)^2}, \text{ for } K_p > 0.6 \quad (66)$$

Where η = single drop collection efficiency, fraction

K_p = inertial impaction parameter, dimensionless

For intermediate Reynolds numbers, single collector collection efficiency can be determined by interpolation between the theoretical curves for viscous and potential flows. Langmuir (1948) gave the following empirical interpolation formula:

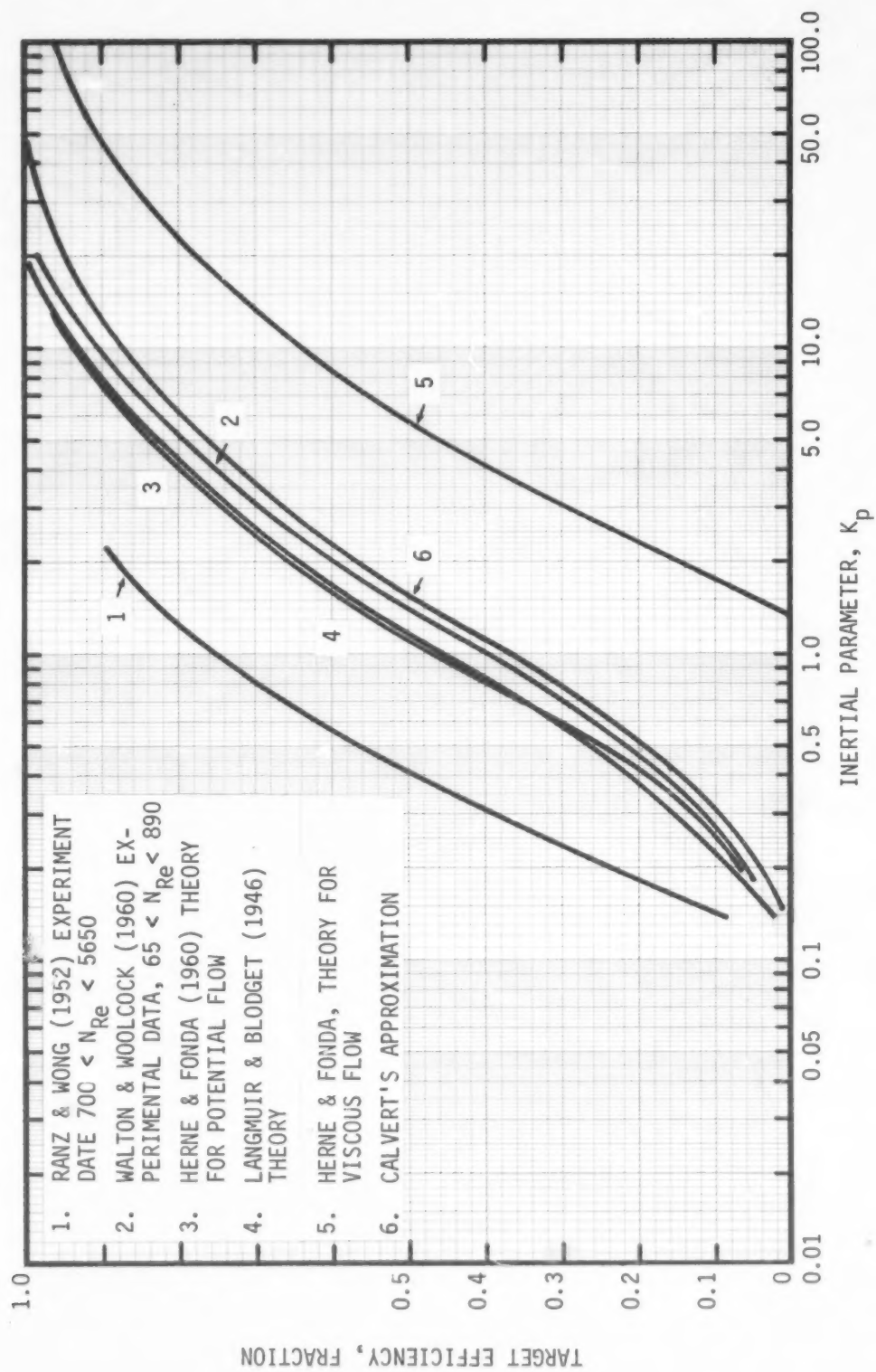


Figure 21. Target efficiency results of various investigators.

$$\eta = \frac{\eta_v + \eta_p \frac{N_{Red}}{60}}{1 + \frac{N_{Red}}{60}} \quad (67)$$

Where η = single drop collection efficiency, fraction

η_v = viscous flow single drop collection efficiency, fraction

η_p = potential flow single drop collection efficiency, fraction

N_{Red} = drop Reynolds number, dimensionless

Crawford (1976) considered the effect of the boundary layer on the collection efficiency and gave elaborate equations for estimating the efficiency. His equations are:

$$\eta = 8.81 N_{Red}^{-0.5} \left[0.26 N_{Red} \left(\frac{y_1}{d_d} \right)^2 - 0.0113 N_{Red}^2 \left(\frac{y_1}{d_d} \right)^4 \right], y_1 < \delta \quad (68)$$

$$\eta = 7.342 N_{Red}^{-0.5}, y_1 = \delta \quad (69)$$

$$\eta = 7.342 N_{Red}^{-0.5} + 2 \frac{\left(\frac{y_1}{d_d} - \frac{\delta}{d_d} \right) \left[3 + 6 \left(\frac{y_1}{d_d} \right) + 4 \left(\frac{y_1}{d_d} \right)^2 \right]}{1 + 2 \left(\frac{y_1}{d_d} \right)}, y > \delta \quad (70)$$

$$\delta = 1.958 \left(\frac{d_d}{N_{Red}^{0.5}} \right) \quad (71)$$

Where η = single drop collection efficiency, fraction

δ = laminar boundary layer thickness, cm

N_{Red} = drop Reynolds number, dimensionless

d_d = drop diameter, cm

y_1 = distance of the limiting trajectory from the drop surface at 90° from the front stagnation point, cm

The distance of the limiting trajectory from the drop surface at 90° from the front stagnation point, " y_1 ", is a function of " K_p " and can be calculated from the equations presented in Table 2.

Single drop collection efficiency has been measured experimentally by Ranz and Wong (1952) and Walton and Woolcock (1960). Their results are plotted in Figure 21 along with theoretical curves.

Data of Ranz and Wong (1952) show a higher efficiency than those predicted from potential flow. The deviations could be due to errors in estimating the average particle size.

Walton and Woolcock (1960) used a methylene blue aerosol (2.5 μm and 5 μm diameter) impacting on a water drop (500 μm , 1,000 μm , and 2,000 μm diameter) at flow velocities of 2.0, 3.9, and 6.7 m/s to measure particle collection efficiencies. The range of " K_p " was 0.06 - 2.18 and the collector Reynolds numbers were 65-890. Their data agree quite well with theoretical calculations of Langmuir and Blodgett (1946) and Herne (1960) for potential flow. That Walton and Woolcock's drop Reynolds numbers often fell substantially below the criterion for potential flow was apparently unimportant. For potential flow and for values of " K_p " greater than 0.2, the experimental values of inertial collection efficiency for spheres can be approximated by the correlation suggested by Calvert (1970):

$$\eta = \left(\frac{K_p}{K_p + 0.7} \right)^2 \quad (72)$$

Case 2. Electrostatically Augmented Drop and/or Particle

When there is electrostatic force, equation (63) in dimensionless form becomes

TABLE 2. EQUATIONS FOR CALCULATING "y₁"

Range	Equation
$0 < K_p \leq 0.016$	$\frac{y_1}{d_d} = \frac{1.25 K_p (1 - 1.71 K_p)^{\frac{1}{3}}}{(1 + 0.855 K_p)^{\frac{1}{3}}} + 2.543 \times 10^{37} \exp\left(-\frac{86.36}{K_p^{0.01625}}\right)$
$0.016 < K_p \leq 0.96$	$\frac{y_1}{d_d} = \exp\left(-\frac{2.51}{K_p^{0.1422}}\right) + 0.03128 \sin 0.151 (\ln 0.625 K_p + 4.60)^{1.661}$ $- 0.00331 \sin 0.6538 (\ln 0.625 K_p + 4.60)^{1.1136}$
$0.96 < K_p \leq 8$	$\frac{y_1}{d_d} = \exp\left(-\frac{2.51}{K_p^{0.1422}}\right) + 0.03128 \sin 0.151 (\ln 0.625 K_p + 4.60)^{1.661}$
$K_p > 8$	$\frac{y_1}{d_d} = 0.1665 - 0.235 \exp(-1.67 K_p^{0.2694})$

$$K_p \frac{du_G^*}{dt^*} = (u_G^* - u_p^*) + K_e f_e \quad (73)$$

- Where
- K_e = electrostatic force parameter, dimensionless
 - f_e = spatial dependence of the electrostatic force in terms of dimensionless coordinates
 - K_p = inertial impaction parameter, dimensionless
 - u_G^* = dimensionless gas velocity, dimensionless
 - u_p^* = dimensionless particle velocity, dimensionless
 - t^* = dimensionless time, dimensionless

The electrical forces on the particle may be due to:

1. The Coulombic force between a charged particle and a charged collector,
2. The electrical image force between a charged particle and a charged collector,
3. The electrical image force between a neutral particle and a charged collector,
4. The force on a charged particle in the presence of a neutral collector by a uniform external electric field directed parallel to the flow field, and
5. The electric dipole interaction force between a neutral particle and a neutral collector, both polarized by a uniform external electric field directed parallel to the flow field.

The dimensionless electric force parameters for the above five conditions are given by

$$K_c = \frac{Q_d Q_p C'}{3 \pi^2 \epsilon_0 \mu_G u_o d_d^2 d_p} \quad (74)$$

$$K_{ic} = \left(\frac{\epsilon_d - \epsilon_0}{\epsilon_p + 2\epsilon_0} \right) \left(\frac{Q_p^2 C'}{3\pi^2 \rho_0 \mu_G u_0 d_d^3 d_p} \right) \quad (75)$$

$$K_{ip} = \left(\frac{\epsilon_p - \epsilon_0}{\epsilon_p + 2\epsilon_0} \right) \left(\frac{2 Q_d^2 d_p^2 C'}{3\pi^2 \epsilon_0 d_d^5 \mu_G u_0} \right) \quad (76)$$

$$K_{ex} = \frac{Q_p E_0 C'}{3\pi \mu_G u_0 d_p} \quad (77)$$

$$K_{icp} = \left(\frac{\epsilon_p - \epsilon_0}{\epsilon_p + 2\epsilon_0} \right) \left(\frac{\epsilon_d - \epsilon_0}{\epsilon_d + 2\epsilon_0} \right)^2 \left(\frac{\epsilon_0 d_p^2 E_0^2 C'}{d_d \mu_G u_0} \right) \quad (78)$$

Where K_C = Coulombic force parameter, dimensionless

K_{ic} = charged particle image force parameter, dimensionless

K_{ip} = charged collector image force parameter, dimensionless

K_{ex} = external electric field force parameter, dimensionless

K_{icp} = electric dipole interaction force parameter, dimensionless

Q_d = drop collector charge, C

Q_p = particle charge, C

C' = Cunningham slip correction factor, dimensionless

d_d = drop diameter, cm

d_p = particle diameter, cm

u_0 = undisturbed upstream velocity, cm/s

E_0 = uniform external electric field strength, V/cm

μ_G = gas viscosity, g/cm-s

ϵ_d = dielectric constant of the drop, F/cm

ϵ_p = dielectric constant of the particle, F/cm

ϵ_0 = dielectric constant of the gas, F/cm

Kraemer and Johnstone (1955) solved equation (73) numerically and obtained approximate collection efficiencies for potential flow and Stokes flow around a spherical collector in the absence of particle inertia. For the collection of a charged aerosol by a charged collector, considering only the Coulombic force, the collection efficiency is:

$$\eta = -4 K_c \quad (79)$$

For the collection of uncharged aerosol particles by a charged spherical collector considering only the induced charge on the particles, the collection efficiency is:

$$\eta = \left(\frac{15}{8} \pi K_{ip} \right)^{0.4} \quad (80)$$

Nielson (1974) and Nielsen and Hill (1976a) solved equation (73) numerically on a digital computer for the collection of inertialess particles on spheres with electrical force. Their calculations reveal that the collection efficiency of inertialess particles under the influence of Coulombic and external electric field forces is the same for potential flow and viscous flow. Their result for collection efficiency under the influence of Coulombic force is identical to that reported by Kraemer and Johnstone (1955). The collection efficiency for external electric field force is:

$$\eta = \left(1 + 2 \frac{\epsilon_d - \epsilon_0}{\epsilon_d + 2\epsilon_0} \right) \left(\frac{K_{ex}}{1 + K_{ex}} \right) \quad (81)$$

The flow field does affect the collection efficiencies for the image force cases. Their results are plotted in Figure 22.

George and Peohlein (1974) numerically calculated the target efficiencies for the collection of fine particles by a single spherical collector in a

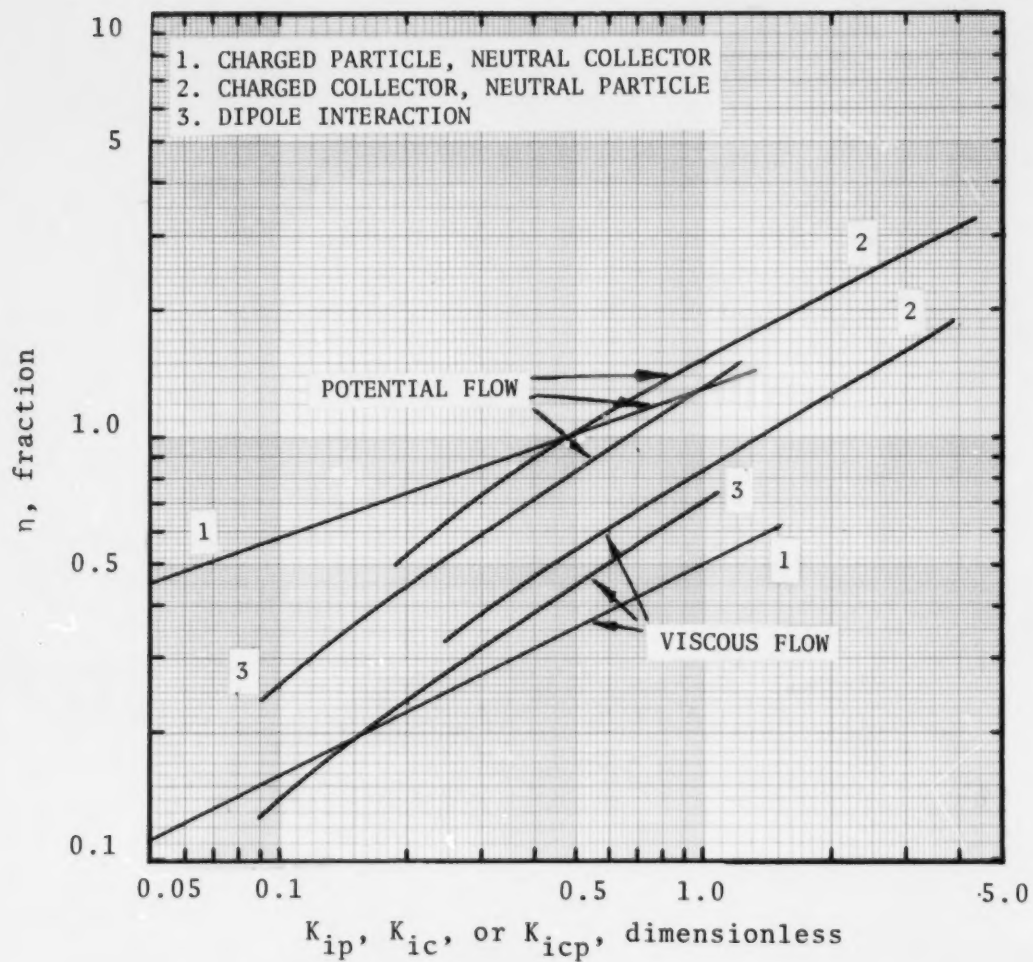


Figure 22. Single collector collection efficiencies for inertialess particles.

gaseous flow field under the combined influences of particle inertia and electrical forces. Their results are presented in Figures 23 and 24.

Nielsen and Hill (1974) demonstrated that the convergence criteria used in George and Poehlein's calculations were not adequate for the computation of some particle trajectories. Nielsen (1974) and Nielsen and Hill (1976b) repeated the calculations. Their results are shown in Figures 25 through 28.

Reported Charged Spray Scrubber Performance Data

The charged spray scrubber has been studied experimentally by a number of researchers, including Lear, et al. (1975), Lear (1976), Pilat (1975), Pilat and Raemhild (1978), Pilat and Meyer (1976), and Pilat, et al. (1978). However, data reported by these researchers are not sufficient for verifying the spray scrubber design equation.

In the Lear, et al. study, charged drops were formed by electrohydrodynamic spraying. The drops were used to collect uncharged particles in the presence of an external electrostatic field.

Pilat, et al. (1974, 1976, 1978a,b) reported test data obtained on bench and pilot scale University of Washington Electrostatic Scrubbers. The scrubber was 12.2 m in length and had two particle charging sections and two spray sections connected in series alternately. Charged drops were formed by mechanical spraying and charged by induction.

Pilat, et al. showed that the collection of charged particles by oppositely charged drops is superior to the collection of neutral particles by neutral drops. However, the reported collection efficiency included the collection in the two particle charging corona sections. The collection by the two charging sections is believed to be so substantial due to long particle residence time as to overshadow the collection by charged drops. Since Pilat, et al. did not present a design model, we find neither an empirical nor an analytical method for designing a scrubber with geometry different than theirs.

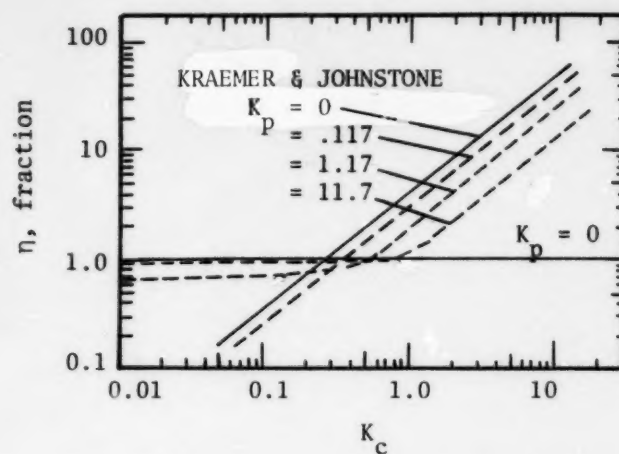


Figure 23. Single drop collection efficiency versus electrostatic parameter with inertial impaction parameter as parameter.

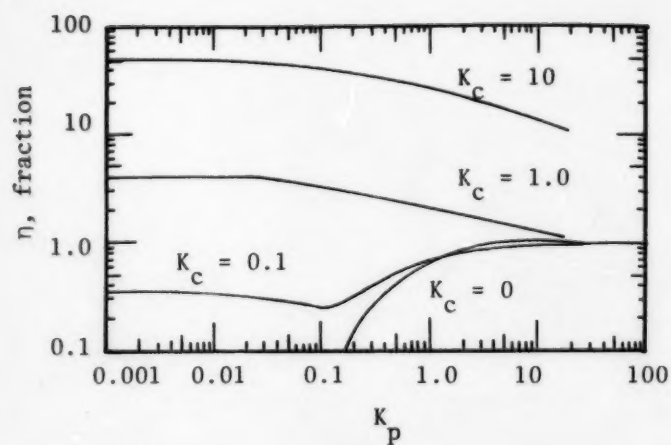


Figure 24. Single drop collection efficiency versus inertial impaction parameter with electrostatic parameter as parameter.

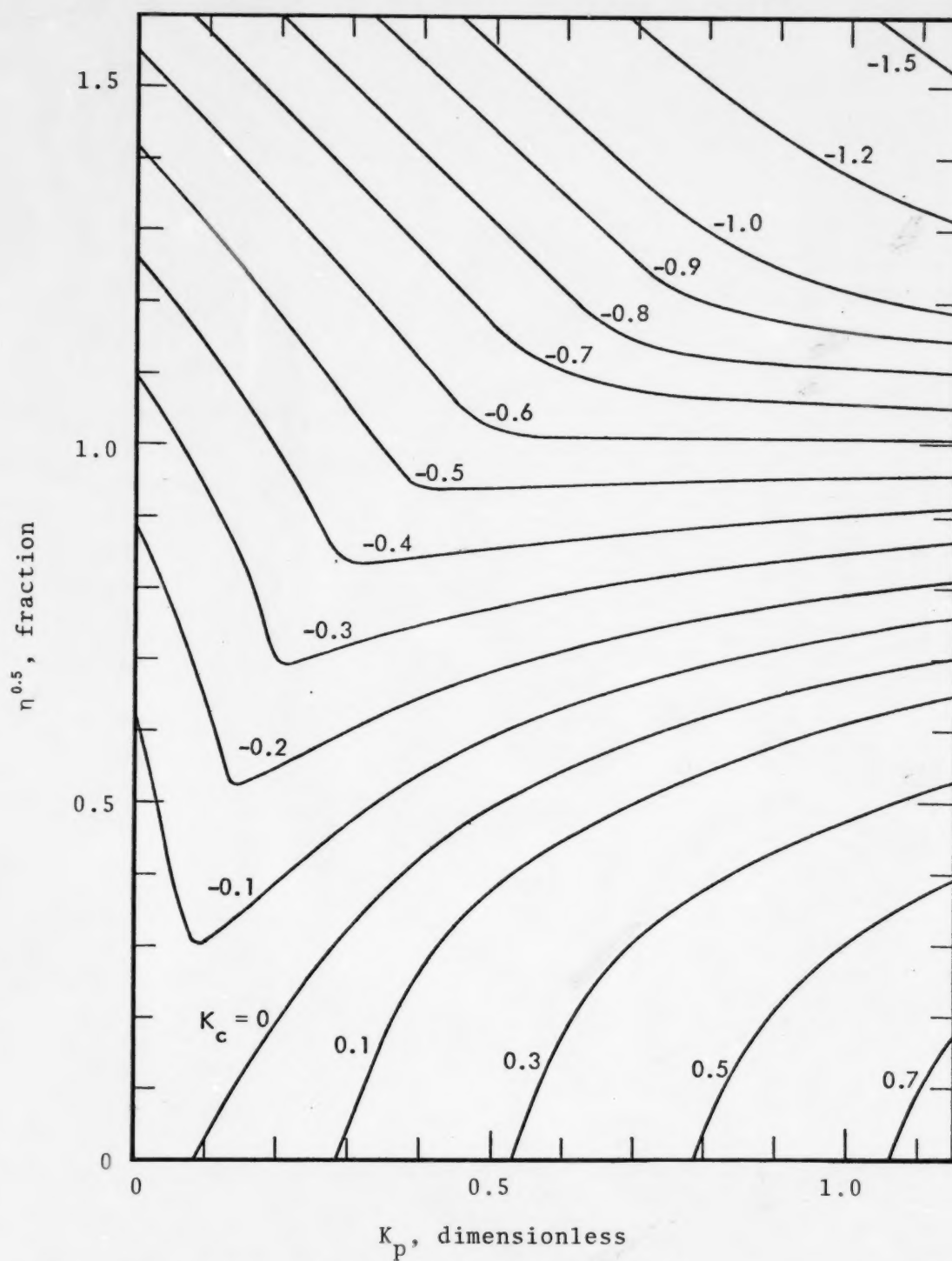


Figure 25. Single drop collection efficiency versus inertial impaction parameter with Coulombic force parameter as parameter.

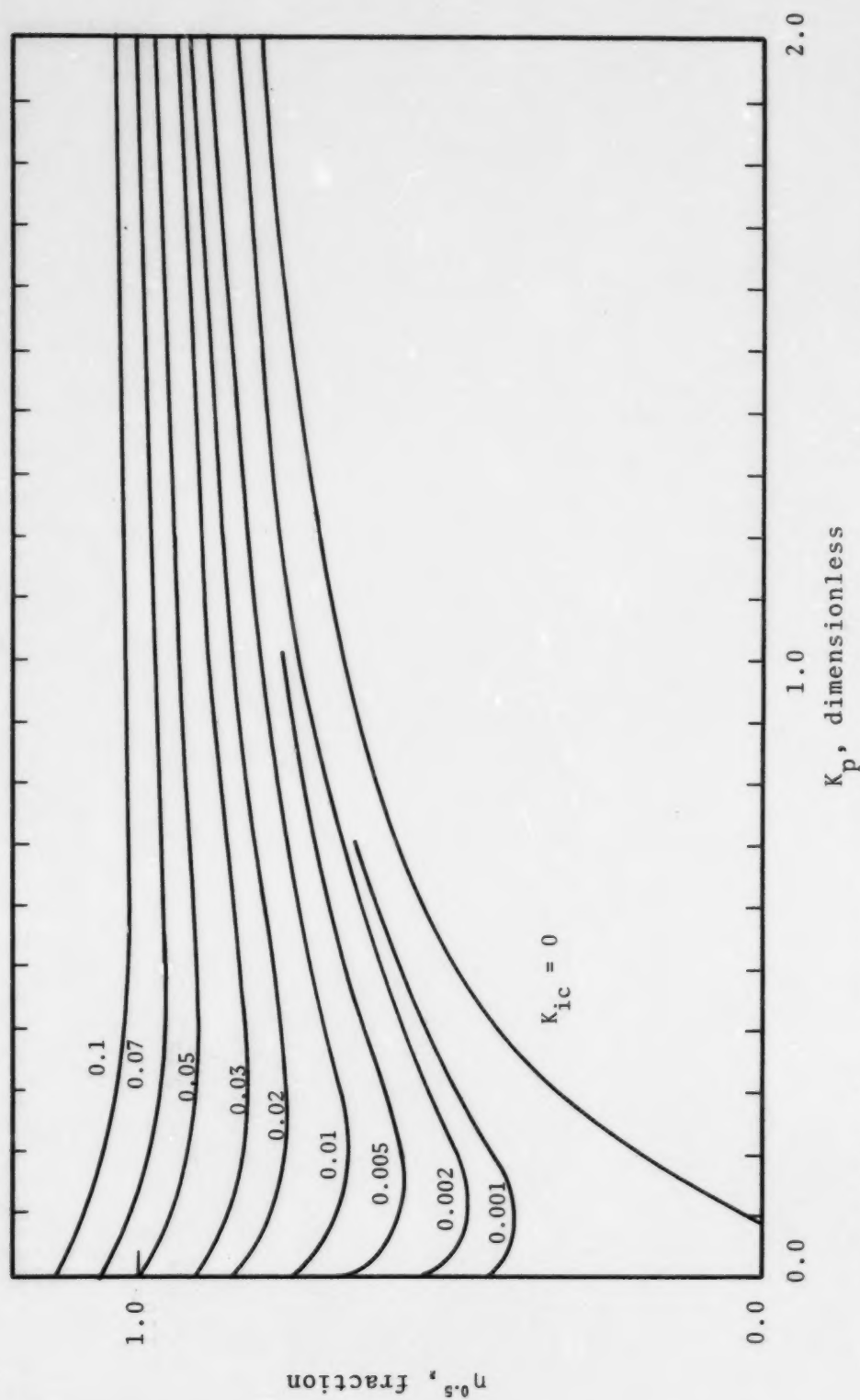


Figure 26 $\eta^{0.5}$ versus K_p for different values of K_{ic} for charged particle image force with potential field.

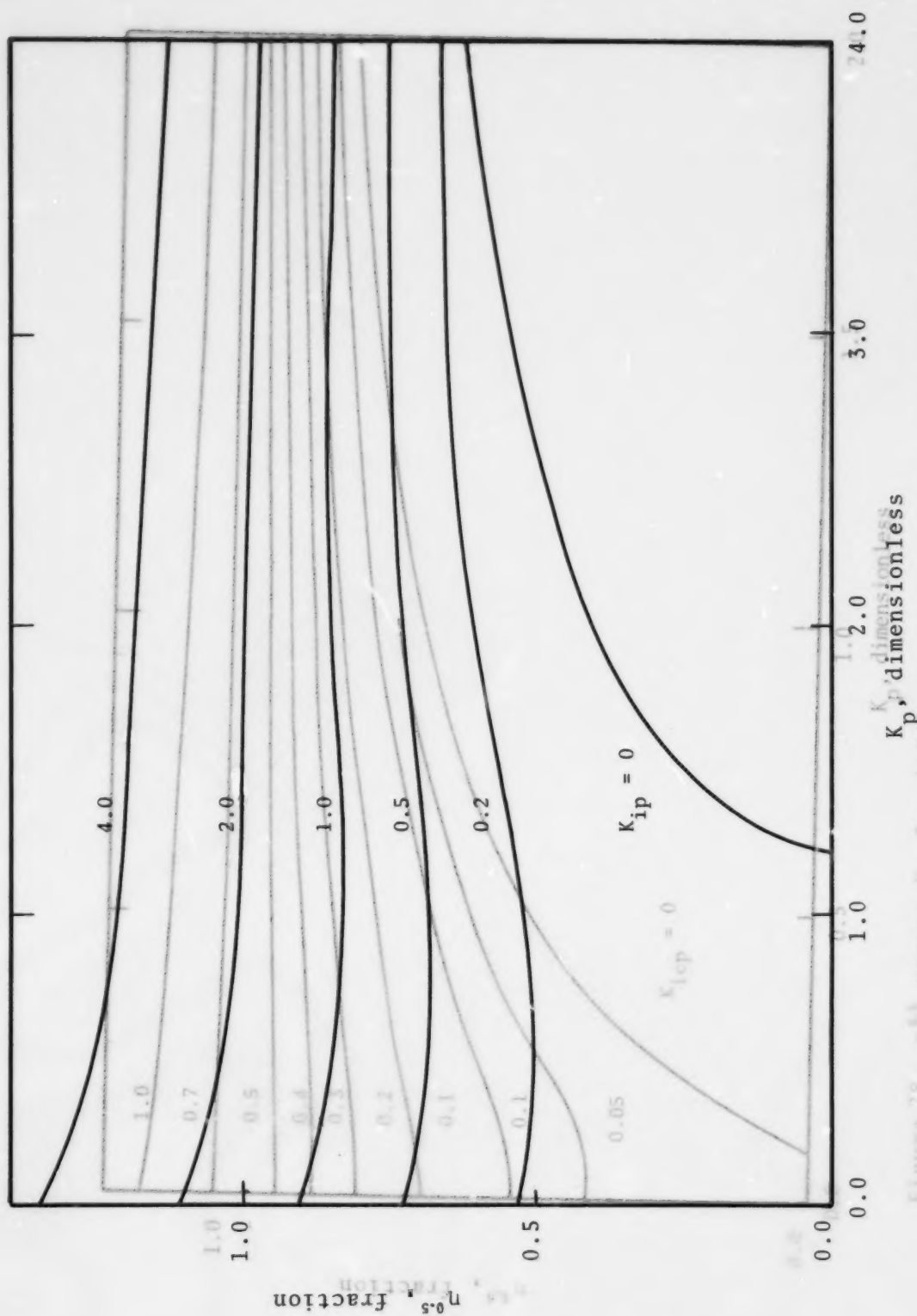


Figure 27. $n^{0.5}$ versus K_p for different values of K_{ip} for the charged collector image force with potential field.

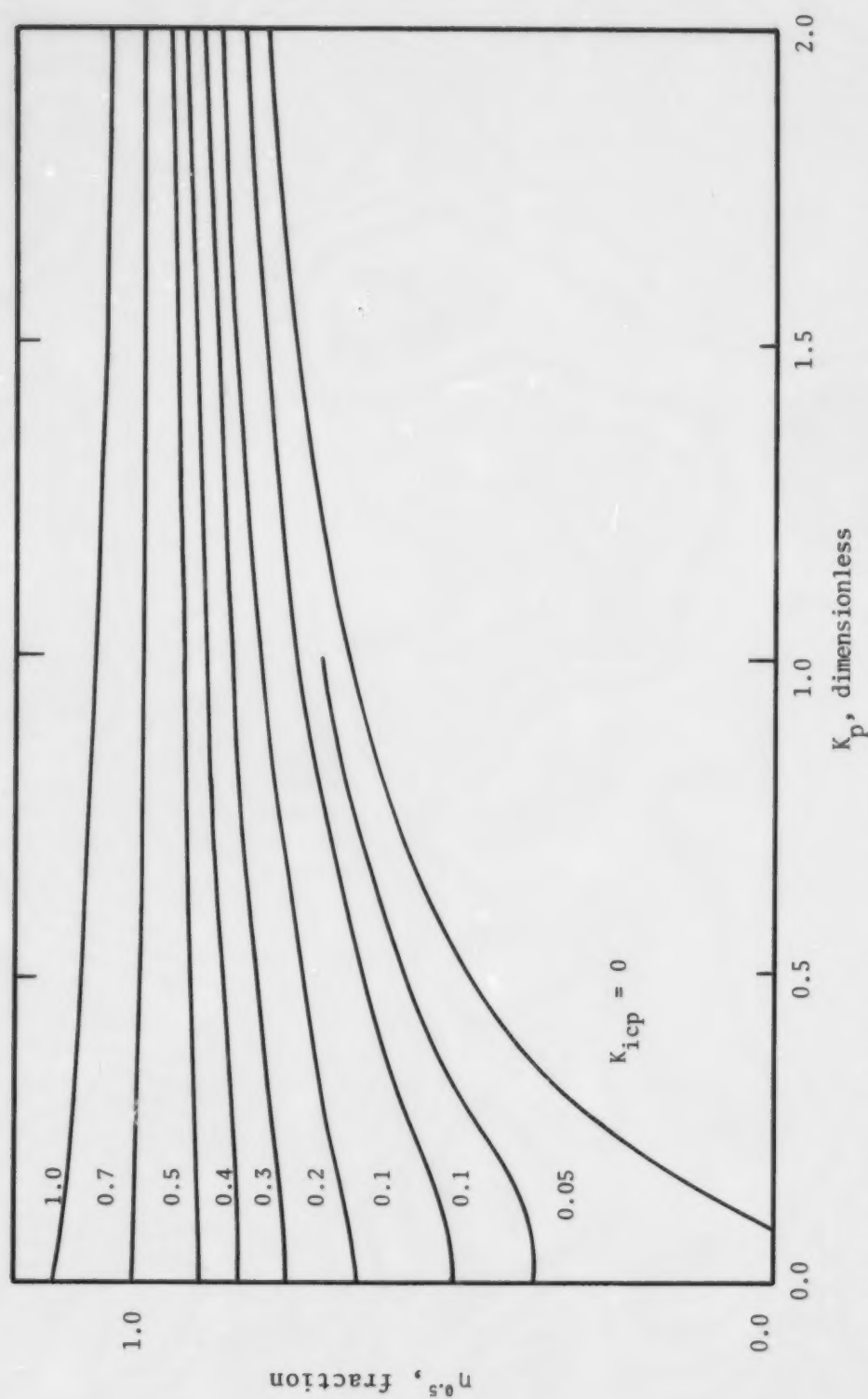


Figure 28. $\eta^{0.5}$ versus K_p for different values of K_{icp} for the electric dipole interaction force with potential flow.

EXPERIMENTS

Scrubber System

In order to obtain performance data under well defined conditions, the charged spray scrubber shown in Figure 29 was built. In order to enable good measurements to be made, the scrubber is much more elaborate than the prototype SCAT will be.

The system was made in sections joined by flanges. The scrubber was made of thin aluminum sheets and supported on PVC frames in order to permit electrical current measurements. The cross section of the scrubber was 0.91 m x 0.91 m (3 ft x 3 ft). The overall length, including the blower, was about 11 m (36 ft).

The scrubber system consisted of a flow straightening section, an inlet particle sampling section, a particle charging section, a spray section, an entrainment separator, and an outlet sampling section. It should be noted that all sections except the spray and entrainment separator section will not be included in an industrial installable SCAT system. The inclusion of the sections in the bench scale device is for the purpose of studying various combinations of components and to provide for good particle sampling and electrical measurement.

The particle charger section consisted of two rows of corona wires. Wire diameter was 0.18 mm (0.007 in.). The spacing between wires within the same row was 6.5 cm (2.5 in.). The ground electrodes were 1.3 cm (0.5 in.) diameter aluminum tubing.

The overall length of the spray section was 2.44 m (8 ft) including two spray banks. The spray banks were removable and their configuration varied depending on nozzle types. Water was supplied to the spray nozzles by an electrically isolated plastic pump. The pump tank was made of polypropylene.

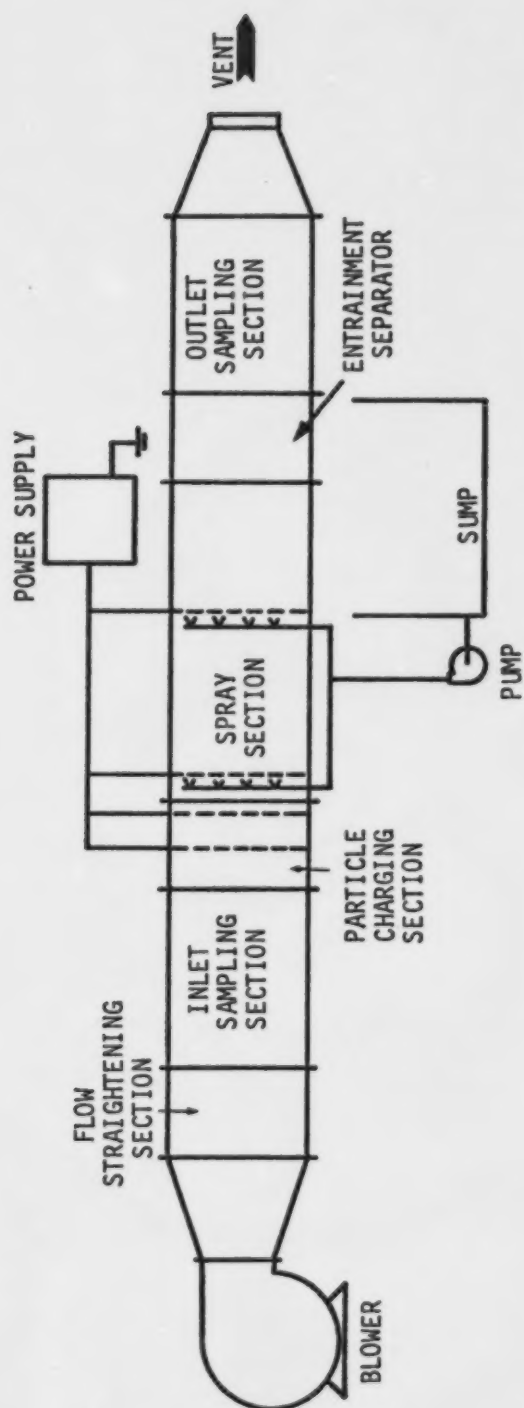


Figure 29 Experimental apparatus for studying charged spray section or SCAT scrubber.

The drops were charged by induction. The system was designed in such a way so that the spray nozzles could either be kept at ground potential or maintained at high voltage. Based on the results of an auxiliary drop charging experiment which is described in the next section, the first method was used because it gave a higher charge/mass ratio. A high voltage grid assembly was placed in front of the grounded nozzles for charging the water drops. The distance between the grid and nozzle was adjusted for maximizing the charge level on the drops.

The charging grid and water supply were isolated from the scrubber shell. They were kept dry with purging air at the point where they passed through the shell.

Drop Charge Experiments

A review of various drop charging methods has been presented by Melcher and Sachar (1971). Liquid drops may be charged by induction or by field charging. The maximum charge on liquid drops can be calculated from the Rayleigh limit

$$n_c = \frac{(2 \pi \sigma_L)^{0.5}}{e} d_d^{1.5} \quad (82)$$

Where n_c = number of elementary charges on drop, number

σ_L = surface tension of liquid, dyne/cm

d_d = drop diameter, cm

e = 4.8×10^{-10} esu

Induction charging method was chosen for the present study because it is simpler. In induction charging, the high voltage terminal can either be connected directly to the nozzle or to a grid around the nozzle. An experimental setup to study induction drop charging method and nozzle types was designed and built. Figure 30 shows the setup. It consisted of a water reservoir

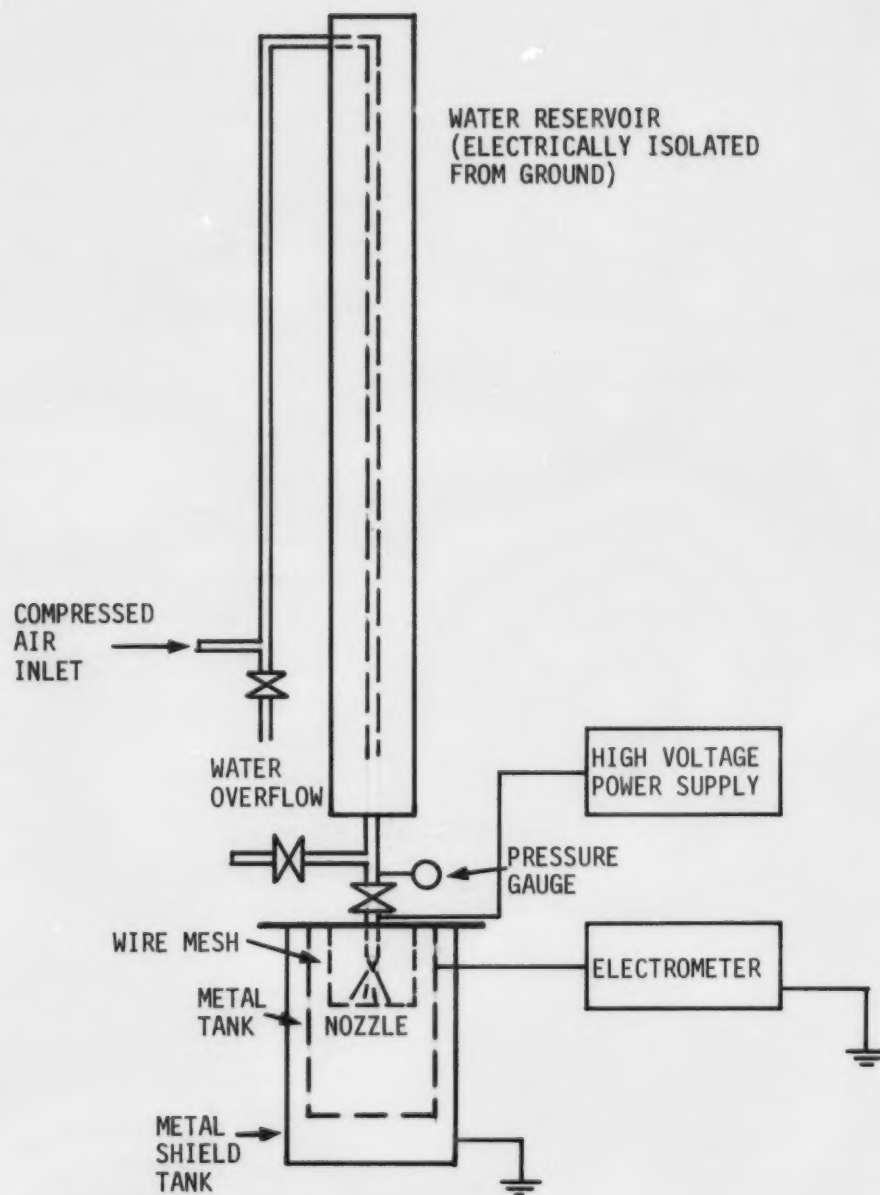


Figure 30. Drop charging experimental setup.

which was electrically isolated from the ground, a spray nozzle, a shielded metal tank, a high voltage power supply, and an electrometer.

In the first series of testing, the high voltage terminal was directly connected to the spray nozzle. The water was sprayed into a shielded metal drum. The charges carried by the water drops would transfer to the metal tank and drain to the ground through an electrometer. By monitoring the current and spray volume, the charges on drops can be calculated.

Experiments were done with a pigtail nozzle (nozzle "C"), a spiral type nozzle (nozzle "B"), a hook type nozzle (nozzle "A") and a Whirljet nozzle (nozzle "D"). In all experiments, the pressure at the nozzle was maintained at 380 kPa (40 psig) by injecting compressed air. At this nozzle pressure, the drops produced by the hook type nozzle are about 250 μm in diameter. Both the pigtail nozzle and the Whirljet nozzle produced drops which are approximately 350-400 μm in diameter. The drops from the spiral type nozzles are about 1,000 μm .

Table 3 shows the measured charge/mass ratio. It seems that the charges imparted to the water drops were independent of nozzle type and drop size. The drops carry charges less than 10 percent of the Rayleigh limit.

One possible explanation for this low charge level may be due to the space charge effect. The charges on drops create an electric field which opposes the charging field and effectively reduces the electric field at the nozzle where the charged drops are formed.

The space charge effect was confirmed experimentally. The spray nozzle in the test apparatus was replaced with a hypodermic needle. The drops from the needle were about 1 mm in diameter. The charge imparted to the drop was measured to be 1.7×10^{-7} C/g, 4.4×10^{-7} C/g, and 9.1×10^{-7} C/g for a charging voltage of 5, 10, and 20 kV, respectively. These charge levels were much higher than that obtained with the spray nozzle. Since the drop concentration produced by the hypodermic needle was much lower than that obtained with the spray nozzle, the space charge effect was smaller with the hypodermic needle.

TABLE 3. CHARGE IMPARTED TO WATER SPRAY (C/g).

Voltage -kVdc	Nozzle Type			
	Spiral (B)	Pigtail (C)	Hook (A)	Whirljet (D)
5	2.0×10^{-8}	4.5×10^{-8}	4.3×10^{-8}	2.7×10^{-8}
10	4.0×10^{-8}	8.7×10^{-8}	1.0×10^{-7}	5.7×10^{-8}
20	1.0×10^{-7}	1.9×10^{-7}	1.7×10^{-7}	1.3×10^{-7}

After these tests, the high voltage terminal was connected to a grid in front of the nozzle. The nozzle was grounded through the electrometer as shown in Figure 31.

Figure 32 shows a plot of measured induced current flow versus applied voltage for several nozzle to grid distances. The nozzle was the hook type (nozzle "A"). It should be noted that the measured current included the charges on the drops and the corona current. Corona discharge occurred at the edges of the liquid sheet when the applied voltage was higher than 10 kVdc.

Charging by this method resulted in a higher charge/mass ratio than that obtained by maintaining the nozzle at high potential (Figure 33). At an applied voltage of 10 kVdc and a grid/nozzle distance of 5.1 cm, the measured charge/mass ratio was 4×10^{-7} C/g which was four times higher than that obtained by charging the nozzle.

One possible explanation for this higher charge per mass with charged grid/grounded nozzle arrangement could be due to the avoiding of space charge effects from previously emitted charged drops downstream. By using the charged grid in close proximity downstream of the spray nozzles, the space charge due to the cloud of previously charged drops is shielded from the nozzles. The electric field in the region between the grid and the nozzle is fixed and relatively unaffected by the small number of charged drops in the region between. The electric field at the surface of the water at the point where it breaks up is proportional to the level of charge on the emitted drops. If the nozzles were at high electric potential with no grid downstream, the cloud of charged drops previously emitted would set up an electric field that would tend to reduce the field at the nozzle surface and thereby reduce the amount of charge on the drops.

Maintaining the nozzles at ground potential would also simplify the scrubber construction since it is not necessary to isolate the water supply system. Only one power supply would be needed to charge the particles and the water drops. By maintaining the corona wire in the particle charger and the grid in

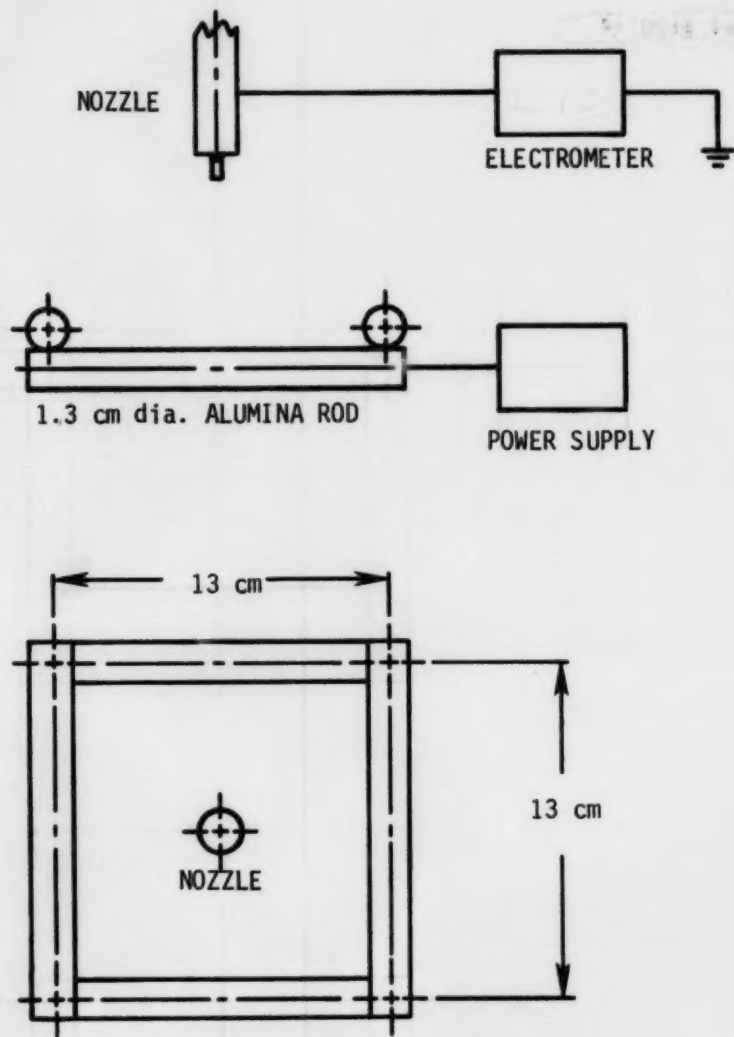


Figure 31. Nozzle/grid arrangement for drop charging.

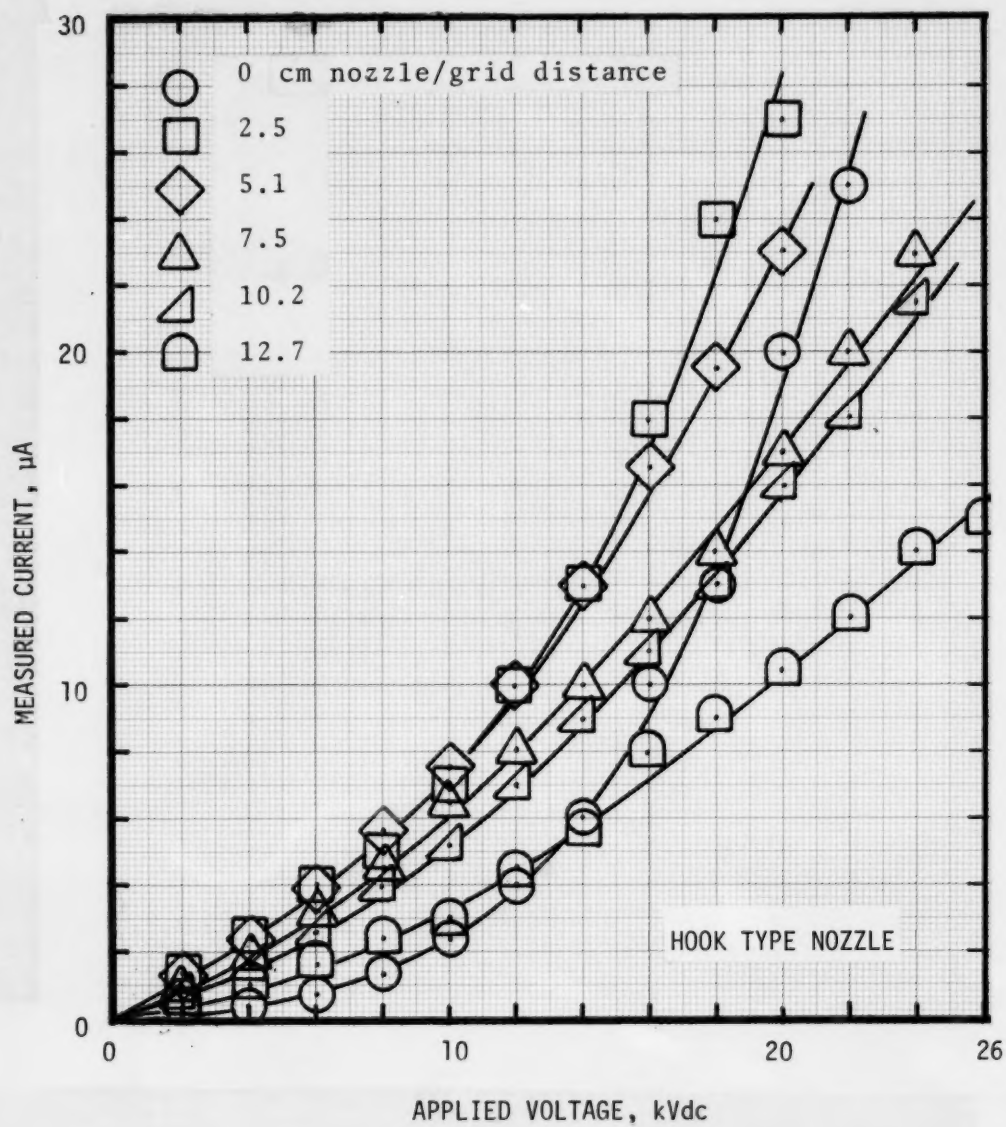


Figure 32. Measured E-1 Curve for Drop Charging.

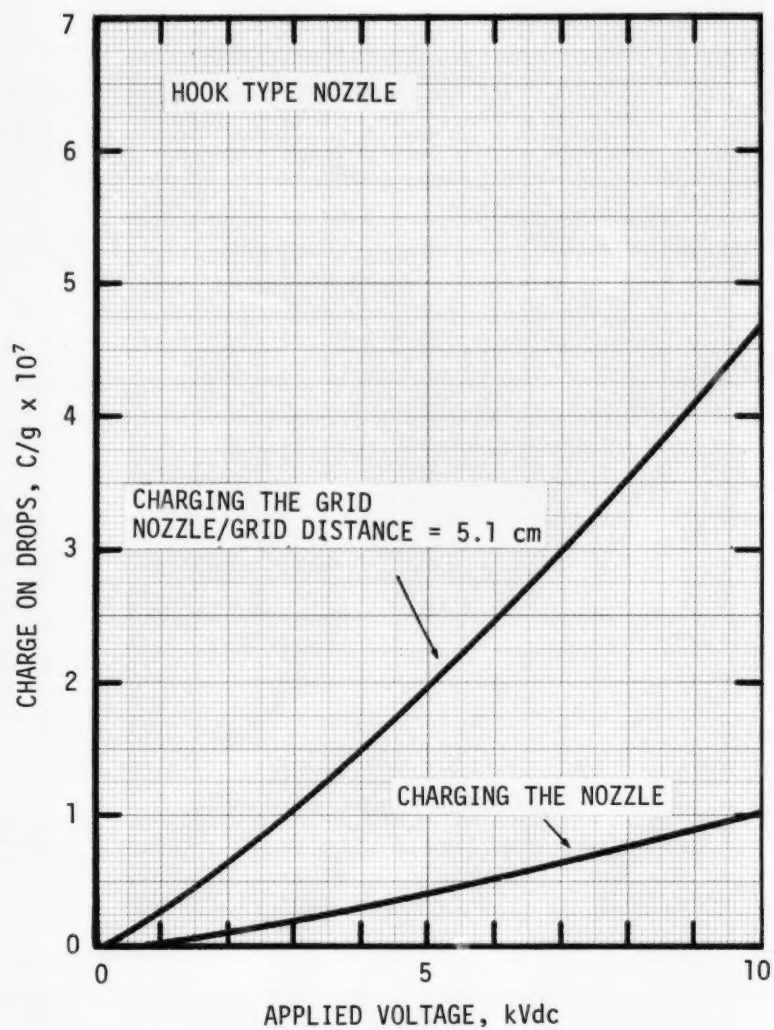


Figure 33. Effect of charging method on charge level on drops.

the spray section at the same polarity, oppositely charged drops and particles would be obtained.

The second drop charging method, i.e., charged grid/grounded nozzle arrangement was used in the spray scrubber system. Drop charge measurements were repeated using the following methods.

1. Monitoring the current output from the power supply. The measured liquid flow rate gives charge/mass ratio for the drop.
2. Grounding the nozzle through an ammeter. Since the scrubber and water lines were electrically isolated from the ground, current flowed from the ground through the ammeter to nozzles and drops. The measured current divided by the water flow rate gives charge/mass ratio for the drops.
3. Placing a drop collector in the scrubber. The collector collects the drops and their charges which are measured by leaking the charge to the ground through an electrometer. Thus, by monitoring the current and sampling time, and measuring the amount of water collected, the charge level can be calculated.

Method 3 is considered to be most accurate, Method 2 is second, and Method 1 is least accurate. Method 1 gives the total energy consumption in charging the water. It includes the charges carried away by the drops and line loss or leakage. In performing the cost analysis, power supply output was used as the basis for energy consumption and either Method 2 or Method 3 was used to determine the charge level on drops.

Figure 34 shows the measured charge level on drops by Method 3, for Nozzle "A". The spacing between the grid plane and the nozzle plane was 1.3 cm (0.5 in.). Curve "A" is for a water flow rate of $9.5 \times 10^{-4} \text{ m}^3/\text{s}$ (15 GPM, 0.31 GPM/nozzle) and a pressure of 450 kPa (50 psig) at the nozzle. Curve "B" is for a water flow rate of $7.2 \times 10^{-4} \text{ m}^3/\text{s}$ (11.5 GPM, 0.29 GPM/nozzle) and a pressure of 380 kPa (40 psig). The drop diameter was measured and sized with a photographic technique to be about 240 μm for both conditions.

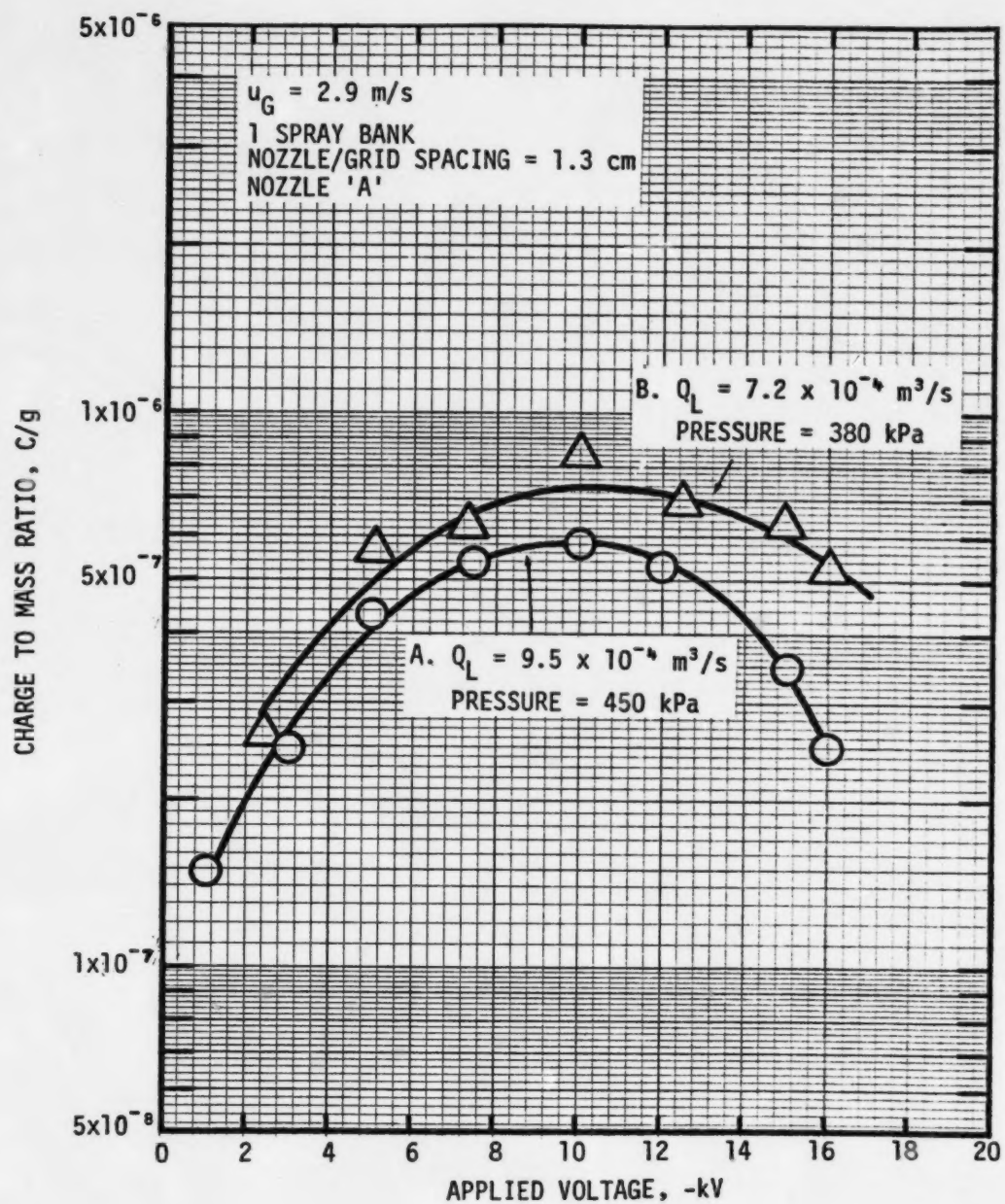


Figure 34. Measured charge level on drops.

Both curves show a maximum at -10 kVdc. When the applied voltage to the grid was below -10 kVdc, the measured drop charge/mass ratio was about the same for the three measurement techniques. When the applied voltage increased above -10 kVdc, power supply current output increased rapidly while the measured charge level by Method 3 decreased. This is illustrated in Table 4 for a water flow rate of $9.5 \times 10^{-4} \text{ m}^3/\text{s}$ (15 GPM).

It is possible that at applied voltage above -10 kVdc, corona discharge occurred at the edge of the liquid sheet. At a larger nozzle/grid spacing, a higher voltage can be applied to the grid without causing corona discharge.

The measured drop charge is in good agreement with that reported by Pilat, et al. (1974). In their experiment, drops were charged by induction with the high voltage terminal connected directly to the nozzle. They used Spraying Systems Fogjet 7N4 nozzles which produce 50 μm diameter drops. At an applied voltage of 5 kVdc, the measured charge level was $5.6 \times 10^{-7} \text{ C/g}$.

Drop Size Distribution

A high speed photographic technique was used for measuring drop size and distribution. The same technique can also be used for measuring the drop velocity. Figure 35 shows the schematic of the setup. It consists of two short duration strobe lights, a 35 mm SLR camera, and a beam splitter.

Lights - Two Edgerton, Germeshausen, and Grier, Inc. (EG&G) Model 549 Microflash electronic flash units were used to provide illumination for the "stop action" photography. These units consist of the light flash unit and the driver unit and can be triggered by a manual push button, by exposure of the photoelectric tube, or by remote contact closure or electrical pulse. The most valuable characteristic of these systems is that they have an extremely short useful flash duration of 0.5 μs . Light quality approximates daylight and peak light is 50×10^6 candelas. The guide number is approximately 4.88 m (16 ft) for ASA 32 film.

TABLE 4. PERCENT OF POWER SUPPLY OUTPUT CURRENT IMPARTED
TO LIQUID DROPS

Applied Voltage -kVdc	Power Supply Current Output, mA	Measure Charge on Drops, C/g	Calculated Total Drop Current, mA	Power Supply Output %
-10	-0.5 to -0.6	$+5.8 \times 10^{-7}$	0.55	100.0
-12	-0.8 to -0.9	$+5.3 \times 10^{-7}$	0.50	59.0
-15	-2.0	$+3.4 \times 10^{-7}$	0.32	16.0
-16	-2.6 to -3.0	$+2.5 \times 10^{-7}$	0.26	9.3
-20	Sparks	--	--	--

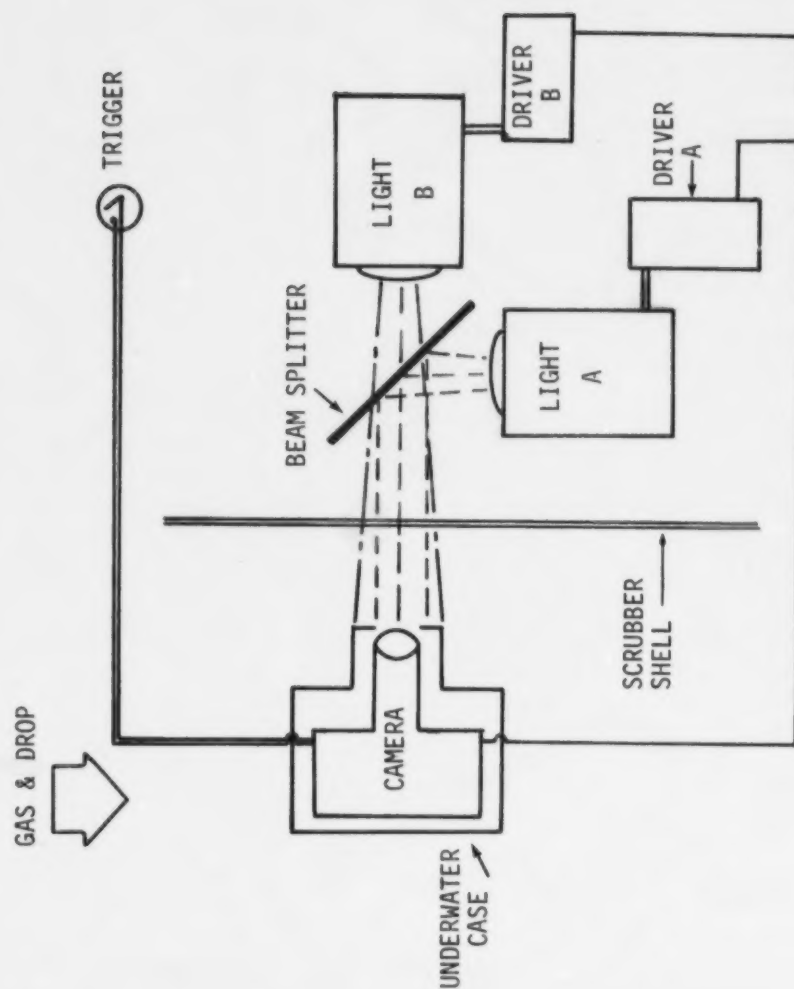


Figure 35. High speed photography schematic.

The flash unit houses the air flash tube, reflector, rectifier circuit, energy-storage capacitor, and trigger transformer. The air flash tube (guided spark-gap light source) contains three wires, two electrodes, and a trigger wire. A 16 kV potential is maintained on the insulated, separated electrodes. The air gap is sufficient so that the tubes can only fire when a trigger impulse of about 70 kV is applied. The tube is mounted at the focal point of a parabolic reflector. However, the nature of tube construction makes it impossible to obtain a completely uniform beam of light from the reflector. This effect is minor but does occasionally need to be recognized.

The driver unit (model 549-21) generates a 2 kV trigger pulse for application to the trigger transformer of the flash unit. It has a time delay control which allows the user to vary the time between applications of the input signal (by one of the three methods mentioned above). The time delay has two selectable ranges: 2-100 μ s and 2-1,000 μ s, which can be varied with a potentiometer knob marked in increments of 0, 1, 2, ... 10. This time delay is uncalibrated.

In measuring drop size and size distribution, one flash unit is used. In measuring drop velocity, double exposure or double flashing during an exposure is used. In order to accurately measure the speed of the drop, the duration between flashes of a double exposure photograph must be accurately known. Thus, the time delay between the triggering pulse of the two flash systems and consequently the flashes must be calibrated.

Calibration of the time delays was accomplished using a Tektronix Model 466 2-channel storage oscilloscope. The inputs to the vertical channels of the oscilloscope were attached to the output triggering pulse of the drive units. The scope triggering was sensitive enough to allow individual calibration of time delays. The results are presented in Table 5.

Camera - A Nikon FM 35 mm SLR camera was used as a film and lens holder. The camera is equipped with a 55 mm f/3.5 micro lens and an extension ring (Nikon PK-13, 27.5 mm) to arrive at a magnification of 1.

TABLE 5. E.G.&G. 540 MICROFLASH TIME DELAY CALIBRATION

Range	Dial	Time, μ s	
		Driver A	Driver B
0.1 ms	0	3	1
	1	10	12
	2	36	36
	3	52	58
	4	69	78
	5	82	92
	6	96	109
	7	102	120
	8	112	126
	9	122	126
	10	128	126
1.0 ms	0	3	1
	1	160	140
	2	370	360
	3	560	580
	4	760	790
	5	900	980
	6	1,100	1,130
	7	1,200	1,300
	8	1,360	1,470
	9	1,490	1,600
	10	1,520	1,680

Camera films of many speeds resolutions, granularities, and developing characteristics, usually for the reproduction of continuous-tone images, are available. In the present study, films capable of resolutions better than 100 lines/mm (10 μ m) were used. Suitable films are Kodak Contrast Process Ortho, Kodak Panatomic-X, Kodak High Contrast Copy Film, and Pan-X. The High Contrast Copy Film (Kodak 5069 HC) which was used in the present study is capable of 630 lines/mm or above.

Beam Splitter - An Edmund Scientific flat glass beam splitter mirror (Edmund Scientific No. 61,097) was used to give high transmission of light and low distortion. The beam splitter reflects 30 percent of light and transmits 70 percent. The mirror was positioned exactly between light reflectors with each light the same distance from mirror to provide as similar as possible double exposures. In addition, the mirror was centered on the line between film, lens, and lights with the mirror edge plumb and perpendicular to that line. Once the mirror was in position, it was kept in place all the time (for single as well as double exposures).

Experimental and Data Analysis Procedures - In measuring drop size and size distribution, only flash unit "B" was used. The camera, which was housed in an underwater case, was placed in the scrubber, and pictures of a stage micrometer were taken. Next, the stage micrometer was removed and the blower and pump were turned on. After steady air and water flows were reached, a minimum of ten pictures were taken.

After the film was developed it was projected on a screen. The drop size and distribution were determined by measuring the diameter on the screen and counting. The drop diameter was measured with a scale which was calibrated by projecting the stage micrometer on the same screen. Only those drops which were in focus were sized and counted. Drops which are in focus will show a black dot at the center. All 10 picture frames were analyzed in this manner and the total drop number was used for calculations.

In determining drop velocity, both flash units were used. The flash unit "B" was used as the initial flash with the time delay set of 0.1 ms - 0, the

minimum time delay. The delay for driver "A" was set at the desired value, depending on size of the field of view and velocity of the drops.

By projecting the picture on a screen, the distance the drop travelled was measured with a calibrated scale. The distance divided by the duration between flashes yields the drop velocity.

Data - Figure 36 shows the drop size distribution for nozzle "A". Nozzle pressure was 380 kPa (40 psig). Superficial gas velocity was 2.9 m/s and liquid/gas ratio was $5.9 \times 10^{-4} \text{ m}^3/\text{m}^3$. Drops were uncharged.

As can be seen from Figure 36 the number median drop diameter is 240 μm and the geometric standard deviation is 1.2.

Drop Velocity Measurements

The drop velocity measured by the photographic technique is the local velocity of the drop. For mathematical modeling, drop velocity at the nozzle exit is more useful. Even though nozzle exit velocity can be predicted from the measured local velocity, it was not done so in this study.

Drop velocity at nozzle exit was measured in the experimental setup shown in Figure 37. The experiment is based on a momentum balance between the gas phase pressure gain in the duct and the momentum of drops at the nozzle exit. The duct (0.58 m^2 cross-sectional area) was open to the atmosphere upstream from the nozzles and it was closed from the atmosphere downstream.

In starting the experiment, the pump was turned on to start the sprays. The gas phase pressure gain across the sprays, nozzle pressure, liquid flow rate, and spray angle were measured and monitored after steady state had been reached.

By assuming that there were no wall losses of drops and that all drop momentum was transferred to the gas, the drop velocity at the nozzle exit and the nozzle discharge coefficient can be calculated from the following equations:

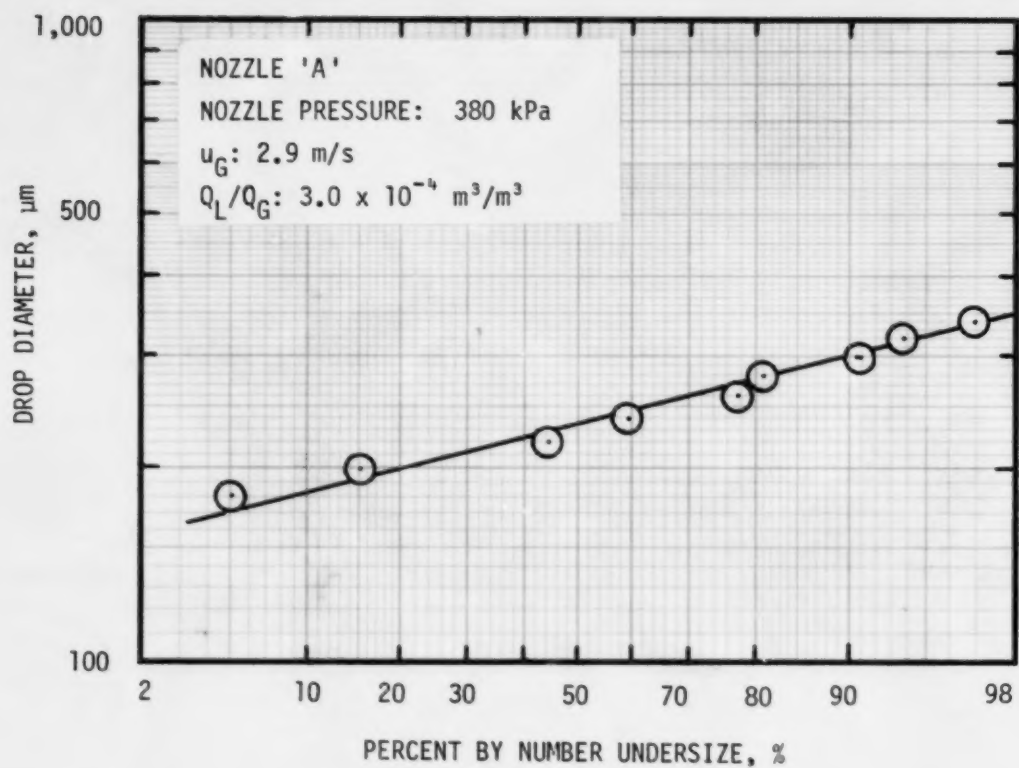


Figure 36. Measured drop size distribution.

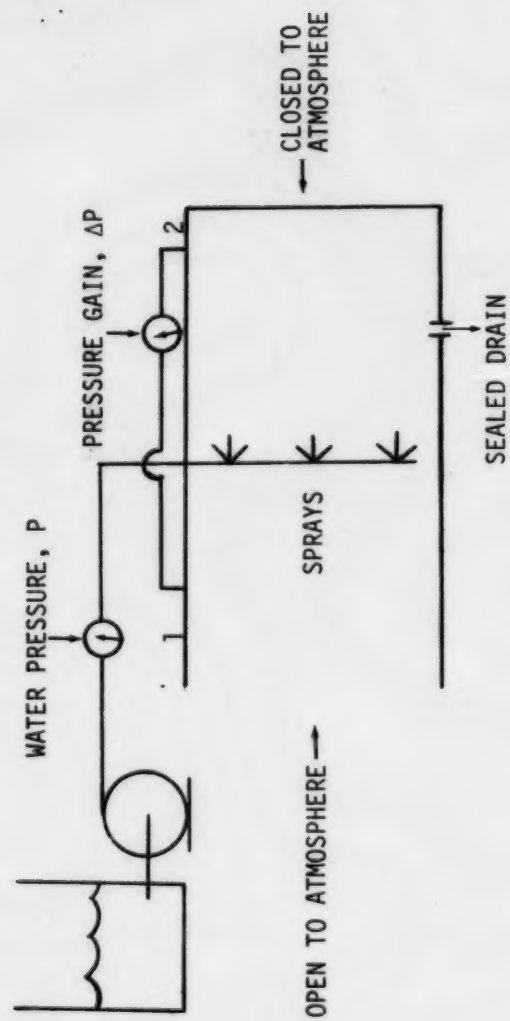


Figure 37. Drop velocity measurement.

$$u_d = \frac{\Delta P_G A}{m_L \cos \frac{\theta}{2}} \quad (83)$$

$$k = u_d \left(\frac{\rho_L}{2 P_L} \right)^{0.5} \quad (48)$$

where u_d = nozzle exit velocity of drops, m/s
 A = duct cross-sectional area, m²
 m_L = mass flow rate of drops, kg/s
 θ = spray angle, degree
 ρ_L = density of liquid, kg/m³
 P_L = nozzle pressure, Pa
 ΔP_G = gas-phase pressure gain, Pa
 k = discharge coefficient, dimensionless

Spray angles and discharge coefficients were measured for six types of nozzles. Data and calculation results are tabulated in Table 6. The average discharge coefficient for all nozzles is about 0.6

Particle Collection Experiments

Particle collection efficiency of the spray scrubber was measured for several scrubber operating conditions. It was determined by injecting test particles into the scrubber and measuring the particle size distributions and mass concentrations simultaneously at the scrubber inlet and outlet with cascade impactors. The particle generation method, data reduction technique, and results are presented in the following sections.

TABLE 6. DROP VELOCITY TEST DATA AND RESULTS

Nozzle	No. of Nozzles	P_L ($\times 10^5 \text{ N/m}^2$)	ΔP_{G2} (N/m^2)	Q_L/Nozzle (cm^3/s)	θ (degrees)	m_L (kg/s)	u_d (m/s)	k
B (pigtail)	20	2.74	14.4	35	87	0.699	17.5	0.71
	20	2.81	14.3	35	87	0.694	16.5	0.69
	20	2.85	14.3	35	87	0.694	16.5	0.69
A (hook type)	20	2.83	8.1	24	100	0.476	15.3	0.64
	20	2.88	8.1	24	100	0.481	15.2	0.63
	20	2.83	8.1	25	100	0.490	14.9	0.63
	20	2.79	8.7	23	100	0.472	16.6	0.71
D (spiral, narrow angle)	16	2.24	34.9	165	45	2.64	8.3	0.39
	16	2.24	34.9	161	45	2.58	8.5	0.40
D (spiral, wide angle)	16	2.36	21.8	151	120	2.42	10.4	0.48
F (internal vane)	16	2.36	23.7	152	120	2.43	11.3	0.52
	20	2.80	14.3	49	65	0.980	10.0	0.42
G (internal vane)	20	2.78	14.2	50	65	0.993	9.8	0.42
	20	2.67	22.4	60	60	1.22	12.3	0.53
	20	2.67	23.7	60	60	1.22	11.5	0.50

Test Particle Generation

Flyash, cupola dust, titanium dioxide, limestone, and hydrated lime particles were used for testing. Test particles were generated by redispersing the powder with a jet of compressed air which was ionized by passing through a Polonium 210 ionizing air nozzle.

In the beginning of a test run, dry powder was loaded into the vibrating hopper and was fed by a screw feeder arrangement into the compressed air jet. The feed rate of the powder was controlled by regulating the screw turning speed through a variable speed motor. The air jet dispersed the powder into airborne particles. The dispersed aerosol next entered the cyclone precutter to remove coarse particles from the dispersed aerosols. The aerosol was then injected into the fan inlet to provide better mixing with the air.

Particle Sampling System

Two identical, modified EPA Method 5 particle sampling trains with in-stack University of Washington Mark III cascade impactors were used to measure the particulate loadings and size distributions in the scrubber gas inlet and outlet streams simultaneously. One point sampling technique was used in the entire experimental program.

In determining the sampling point, the following procedure was used. The velocity profile and particle distribution in the duct were first determined with an anemometer and several simultaneous filters, respectively. The location, which had a velocity and particle concentration close to the average, was then determined. The sampling nozzle was kept at this location and isokinetic condition was maintained for the entire run.

The above procedure was used only when the volumetric gas flow rate and the liquid/gas ratio were changed because it was found that velocity profile and particle distribution did not change much from experiment to experiment with the same liquid and gas flow rates.

The collection plates of the cascade impactor were lined with greased (Apeizon grease) aluminum foils. The substrate preparation and handling procedures were that recommended by EPA (Harris, 1977).

Data Reduction Methods

Particle Size Distribution - The particle size distribution were calculated from the cascade impactor stage weight gains which were measured gravimetrically.

The particle diameter measured by an impactor is called "aerodynamic diameter" and it has the units of "aerodynamic micrometers, μmA ." This is the effective diameter for particle separation by inertial impaction and it takes into account the effects of particle density and particle slip between gas molecules. Aerodynamic diameter is related to geometric diameter (actual size) by the following relationship (Calvert, 1970):

$$d_{pa} = d_p (C' \rho_p)^{0.5} \quad (85)$$

where: d_{pa} = aerodynamic diameter, $\mu\text{mA} \equiv \mu\text{m} (\text{g}/\text{cm}^3)^{0.5}$

d_p = actual diameter, μm

C' = Cunningham slip factor, dimensionless

ρ_p = particle density, g/cm^3

The Cunningham slip factor is a complex function of the mean free path of the gas and the particle diameter. It increases as temperature increases and as pressure decreases. For air at standard temperature and pressure it is given approximately by:

$$C' = 1 + \frac{0.165}{d_p} \quad (86)$$

Cumulative mass of particles collected on a stage and all the stages below, including the absolute filter, were calculated as a percentage of the total weight gain. The cut diameters for the impactor stages were determined from the sampling flow rate and impactor calibration.

The particles were assumed to have a log-normal distribution. The cascade impactor stage cut diameters were plotted against the percent by weight of particles smaller than the cut diameters on log-probability paper. A straight line was fitted to the data points. The 50% value of " d_{pa} " is the geometric mass median diameter, " d_{pg} " and the 84.1% value of " d_{pa} " divided by the mass median diameter is equal to the geometric standard deviation, " σ_g ".

Particle Loadings and Overall Penetrations - The total loadings in the inlet and outlet ducts were calculated in the following manner:

1. The sample flow rate was converted to the normal conditions of 20°C and 76 cm of mercury pressure.
2. Total weight gain on the sampling elements was measured with an analytical balance.
3. The particle mass concentration, c_p (g/DNm³), was calculated from:

$$c_p = \frac{(\text{Total weight gain, g})}{(\text{Sampling rate, DNm}^3/\text{min}) \times (\text{Sampling time, min})}$$

4. The overall penetration was calculated from:

$$\overline{Pt} = \frac{c_{po}}{c_{pi}} \quad (87)$$

where " c_{po} " and " c_{pi} " are the outlet and inlet particle concentrations measured simultaneously for the run.

Fractional Penetrations - The penetration as a function of particle aerodynamic diameter, or the grade penetration through the scrubber, was calculated based on the following equations.

Overall penetration is defined as:

$$\overline{P\tau} = \frac{1}{c_{pi}} \int_0^{c_p} P\tau_d \, dc_p \quad (88)$$

where " c_{pi} " is the inlet particle concentration and " $P\tau_d$ " is the penetration for particle diameter " d_{pa} " and it is given by:

$$P\tau_d = \frac{f(d_{pa})_0}{f(d_{pa})_i} = \frac{\left[\frac{dc_p}{d(d_{pa})} \right]_0}{\left[\frac{dc_p}{d(d_{pa})} \right]_i} \quad (89)$$

where $\frac{dc_p}{d(d_{pa})}$ is the slope of cumulative mass concentration less than " d_{pa} " versus the aerodynamic particle diameter curve at " d_{pa} ", and equals " $f(d_{pa})$ ".

The slopes of the cumulative mass plot were determined by three methods:

1. visual judgment,
2. spline fit, and
3. finite difference.

Visual Judgment - Cumulative mass concentrations were plotted against particle diameters. A curve was fitted to the data points visually. Slopes were then obtained graphically.

Spline Fit - A cubic spline function was used to estimate the derivatives at the data points. The derivatives for other points were calculated by interpolation. Details of this technique has been presented by Lawless (1978).

Finite Difference - This method was discussed in detail by Smith and Cushing (1977). The derivative is calculated by drawing a straight line between the two neighboring data points. The derivative is assumed to be that at the geometric mean of the two neighboring particle diameters. This procedure is repeated for all data points. Slopes for other diameters are calculated by interpolation.

Slopes of the inlet and outlet plots were determined for several " d_{pa} " values in the range of 0.4 to 10 μm . The fractional penetrations were then determined from the ratio of the slopes, as described above. The fractional penetrations were plotted against particle diameters. The curve is termed grade penetration curve.

The grade penetration curve for each run was determined by all three methods. In general, the curves obtained by visual and finite difference methods were close to each other. The curve determined by the spline fit method had peaks and valleys. The grade penetration curve presented in this report was the average of the three methods. A smooth curve was fitted to the calculated penetration points to eliminate fluctuations.

Particle Charge Measurement

Charge level on particles was measured with a Faraday cup which consisted of a shielded and isolated glass fiber filter connected to an electrometer. The filter collects the particles and their charges which are measured by leaking the charges to the ground through the electrometer. The measured charge divided by the weight of the collected particles gives the charge-to-mass ratio.

Particle Data

Two types of nozzles ("A" and "B") were tested. Nozzle "A" is a hook type nozzle. The mass median drop diameter, discharge coefficient, and spray angle of this nozzle type were measured to be 240 μm , 0.65, and 100°, respectively. Nozzle "B" is pigtail type nozzle. The mass median diameter, discharge coefficient, and spray angle were measured to be 400 μm , 0.70, and 87°, respectively.

All experiments were done under room temperatures and pressures. The particle collection efficiency of the spray scrubber was measured for the following four conditions:

1. ND/UP (neutral drop/uncharged particle).
2. ND/CP (neutral drop/charged particle).
3. CD/UP (charged drop/uncharged particle).
4. CD/CP (charged drop/charged particle).

Since the main objective of the experiments was to verify the scrubber design equation, one spray bank was used in all tests. The liquid/gas ratio was also quite low for the experiments. Thirty runs were done. Table 7 shows the test summary. Particle data are summarized in Table 8. Particle collection data have been reduced into plots of particle penetration versus particle diameter for all experimental runs. Figures 38 through 50 are the plots. Reduced cascade impactor data are tabulated in Appendix "A".

DISCUSSIONS

Figures 51 through 53 show the effect of particle charging and/or drop charging on scrubber collection efficiency. Scrubber efficiency improves by charging either the water or the particles. Further improvement was obtained when the drops and the particles were oppositely charged. The improvement is more with submicrometer particles. For particles with diameters larger than 5 μm , charging the water and/or particles has small effects on efficiency.

TABLE 7. SPRAY SCRUBBER TEST SUMMARY

Run No.	u_G , m/s	Q_L/Q_G , m^3/m	No. of Spray Bank	Spray Nozzle	Nozzle Pressure, kPa	Particle		Test Particles
						Charging Voltage, -kVdc	Drop Charging Voltage, -kVdc	
70/8/6	2.9	4×10^{-4}	1	A	450	0	0	Fly ash
70/8/7	2.9	4×10^{-4}	1	A	450	0	10	Fly ash
70/8/8	2.9	4×10^{-4}	1	A	450	0	10	Fly ash
70/8/9	2.9	4×10^{-4}	1	A	450	0	10	Fly ash
70/8/10	2.9	4×10^{-4}	1	A	450	0	10	Fly ash
70/8/11	2.9	4×10^{-4}	1	A	450	0	0	Fly ash
70/8/14	2.9	2.9×10^{-4}	1	A	450	0	10	Cupola dust
70/8/15	2.9	2.9×10^{-4}	1	A	450	0	10	Limestone
70/8/18	2.9	2.9×10^{-4}	1	A	450	0	10	Limestone
70/8/19	2.9	2.9×10^{-4}	1	A	450	0	0	Limestone
70/9/1	2.9	4×10^{-4}	1	A	450	0	0	Fly ash
70/9/2	2.9	4×10^{-4}	1	A	450	0	0	Titanium dioxide
70/20/1	2.9	7×10^{-4}	1	B	428	0	0	Hydrated lime
70/20/5	2.9	7×10^{-4}	1	B	428	10	10	Hydrated lime
70/20/7	2.9	7×10^{-4}	1	B	428	0	10	Hydrated lime
70/20/8	2.9	7×10^{-4}	1	B	428	0	10	Hydrated lime
70/20/9	2.9	7×10^{-4}	1	B	428	0	10	Hydrated lime
70/29/10	2.9	7×10^{-4}	1	B	428	10	0	Hydrated lime

TABLE 7. SPRAY SCRUBBER TEST SUMMARY (Continued)

Run No.	u_G , m/s	Q_L/Q_G , m^3/m	No. of Spray Bank	Spray Nozzle	Nozzle Pressure, kPa	Particle Charging Voltage, -kVdc	Drop Charging Voltage, -kVdc	Test Particles
72/9/3	2.7	4×10^{-4}	1	B	300	10	10	Hydrated lime
72/9/4	2.7	4×10^{-4}	1	B	300	10	0	Hydrated lime
72/10/1	2.9	4×10^{-4}	1	A	450	0	0	Hydrated lime
72/10/4	2.9	4×10^{-4}	1	A	450	0	0	Hydrated lime
72/10/16	2.9	4×10^{-4}	1	A	450	10	10	Hydrated lime
72/10/18	2.9	4×10^{-4}	1	A	450	0	10	Hydrated lime
72/10/19	2.7	4×10^{-4}	1	A	450	14.8	9	Hydrated lime
72/10/20	2.7	4×10^{-4}	1	A	450	15.5	8	Hydrated lime
72/10/21	2.7	4×10^{-4}	1	A	450	16	8	Hydrated lime
72/10/22	2.7	4×10^{-4}	1	A	450	15	10	Hydrated lime
72/10/23	2.7	4×10^{-4}	1	A	450	15	10	Hydrated lime
72/10/24	2.7	4×10^{-4}	1	A	450	15	10	Hydrated lime

TABLE 8. SUMMARY OF PARTICLE DATA

Run No.	Inlet Particle Size Distribution		Mass Concentration, mg/DNm ³		Pt, %	Particle Charge	
	d _{pg} , μ m	σ_g	Inlet	Outlet		C/g	Drop Charge, C/g
70/8/6	4.4	1.7	58.5	22.9	0.39	0	0
70/8/7	6.5	2.7	116.7	33.3	0.29	0	5.8×10^{-7}
70/8/8	16.8	5.8	256.3	28.3	0.11	0	5.8×10^{-7}
70/8/9	8.4	3.4	104.7	17.1	0.16	0	5.8×10^{-7}
70/8/10	12.1	4.2	137.1	10.0	0.15	0	5.8×10^{-7}
70/8/11	6.5	3.5	72.9	24.9	0.34	0	0
70/8/14	17.1	3.4	100.4	10.6	0.11	0	5.8×10^{-7}
70/8/15	3.8	2.3	21.2	9.4	0.47	0	5.8×10^{-7}
70/8/18	3.5	1.6	21.0	9.8	0.47	0	5.8×10^{-7}
70/8/19	3.8	1.7	16.6	8.6	0.52	0	5.8×10^{-7}
70/9/1	7.1	2.1	58.6	7.2	0.12	0	0
70/9/2	4.0	2.9	14.7	6.9	0.47	0	0
70/20/1	14.8	5.0	151.6	27.2	0.18	0	0
70/20/5	2.6	2.3	25.4	10.1	0.4	1.5×10^{-5}	5.8×10^{-7}
70/20/7	14.5	3.1	124.3	15.3	0.12	0	5.8×10^{-7}

TABLE 8. SUMMARY OF PARTICLE DATA (Continued)

Run No.	Inlet Particle Size Distribution		Mass Concentration, mg/DNm ³		Pt, %	Particle Charge		Drop Charge, C/g
	d _{pg} , μmA	σ _g	Inlet	Outlet		C/g	C/g	
70/20/8	2.7	2.3	22.6	6.6	0.29	0	5.8 × 10 ⁻⁷	
70/20/9	3.1	2.7	32.1	14.5	0.45	0	0	
70/20/10	6.0	3.5	53.2	11.4	0.21	1.5 × 10 ⁻⁵	0	
72/9/3						1.5 × 10 ⁻⁵	5.8 × 10 ⁻⁷	
72/9/4						1.5 × 10 ⁻⁵	0	
72/10/1	2.0	2.3	28.1	16.2	0.52	0	0	
72/10/4	2.3	2.0	134.0	22.0	0.16	0	0	
72/10/16	15.6	4.3	341.7	47.1	0.14	1.5 × 10 ⁻⁵	5.8 × 10 ⁻⁷	
72/10/18	13.7	4.5	335.5	46.8	0.14	0	5.8 × 10 ⁻⁷	
72/10/19	12.1	5.0	212.3	26.8	0.13	1.1 × 10 ⁻⁵	5.8 × 10 ⁻⁷	
72/10/20	10.8	3.4	353.5	23.5	0.07	1.1 × 10 ⁻⁵	5.8 × 10 ⁻⁷	
72/10/21	17.5	4.8	363.1	29.5	0.08	1.1 × 10 ⁻⁵	5.8 × 10 ⁻⁷	
72/10/22	2.0	2.1	31.5	14.2	0.45	1.1 × 10 ⁻⁵	5.8 × 10 ⁻⁷	
72/10/24	2.0	2.3	18.4	8.5	0.46	1.1 × 10 ⁻⁵	5.8 × 10 ⁻⁷ *	

*Inlet sample was taken at charger outlet

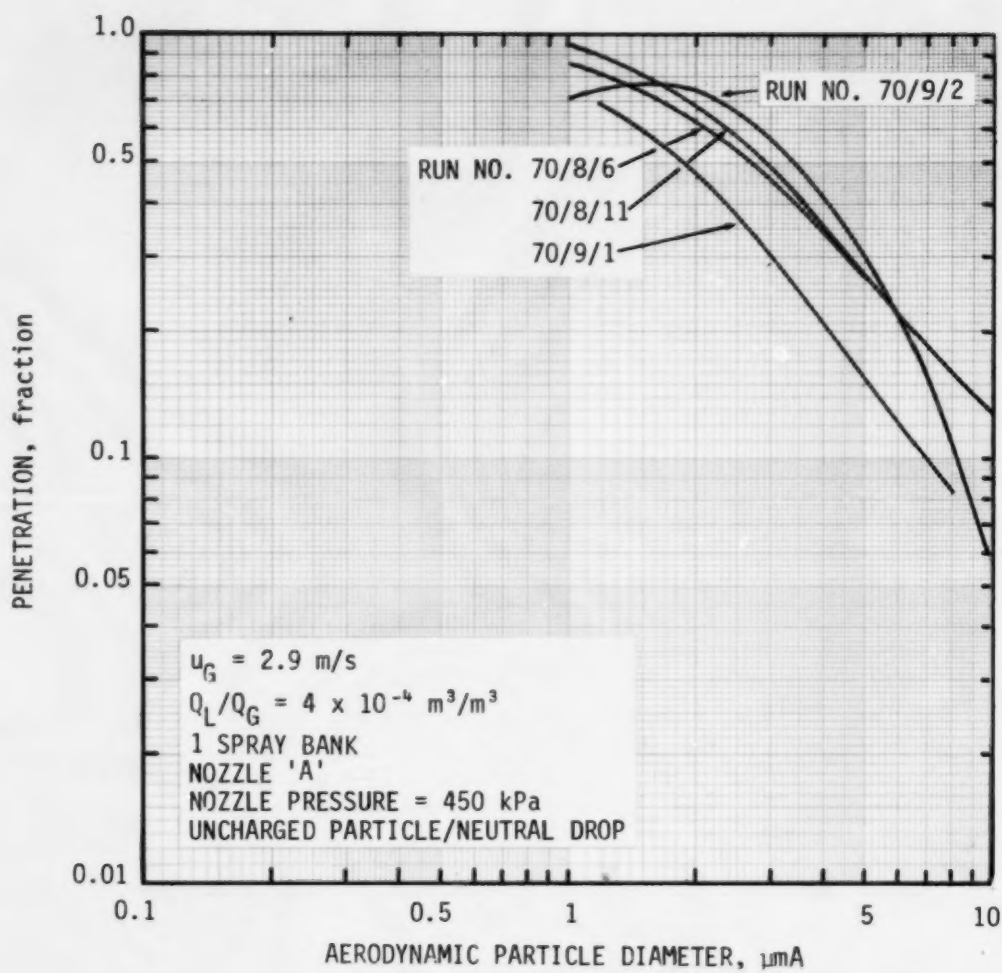


Figure 38. Experimental spray scrubber penetration.

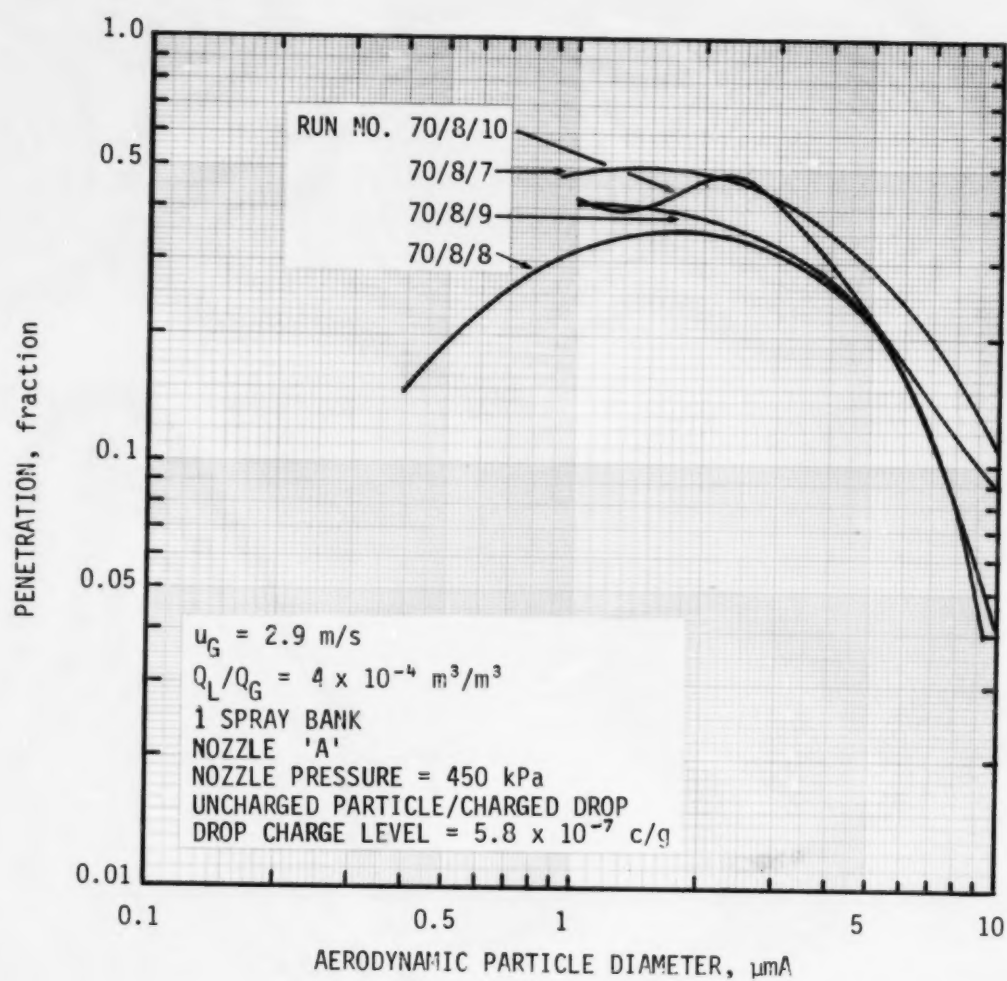


Figure 39. Experimental spray scrubber penetration.

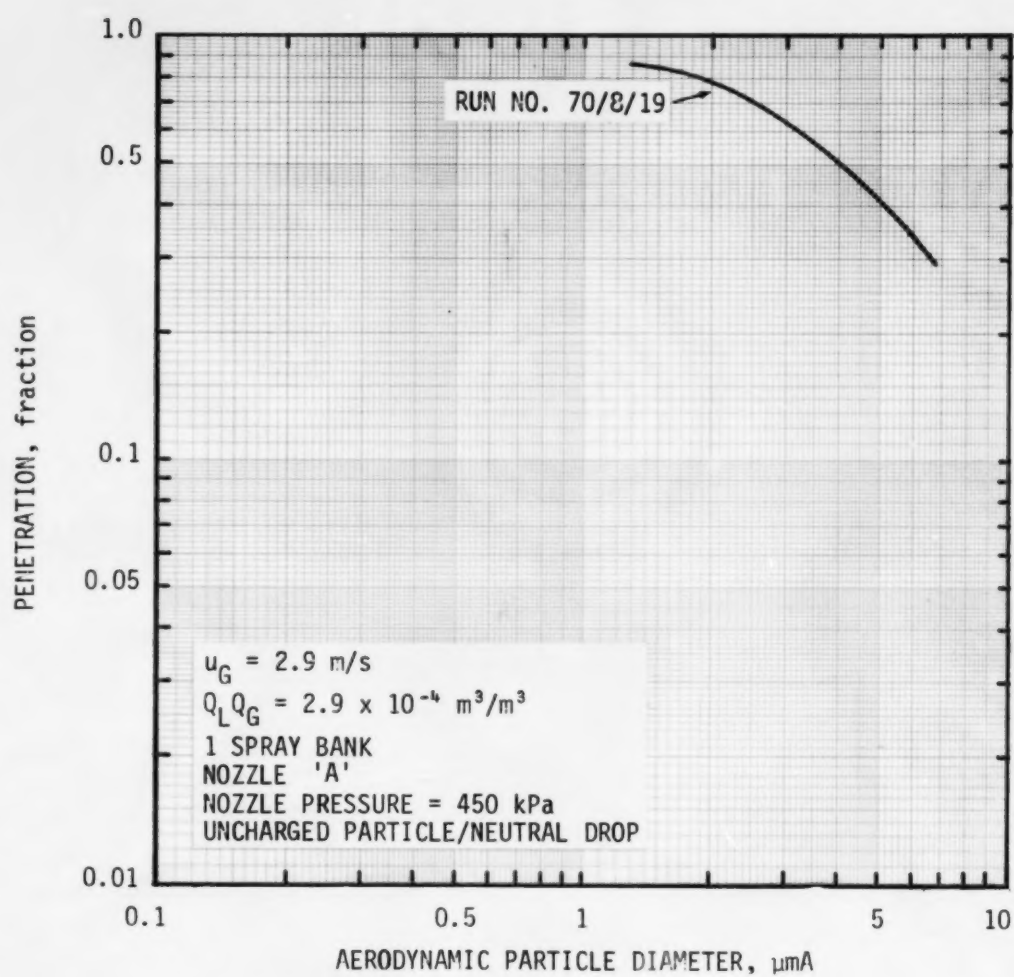


Figure 40. Experimental spray scrubber penetration.

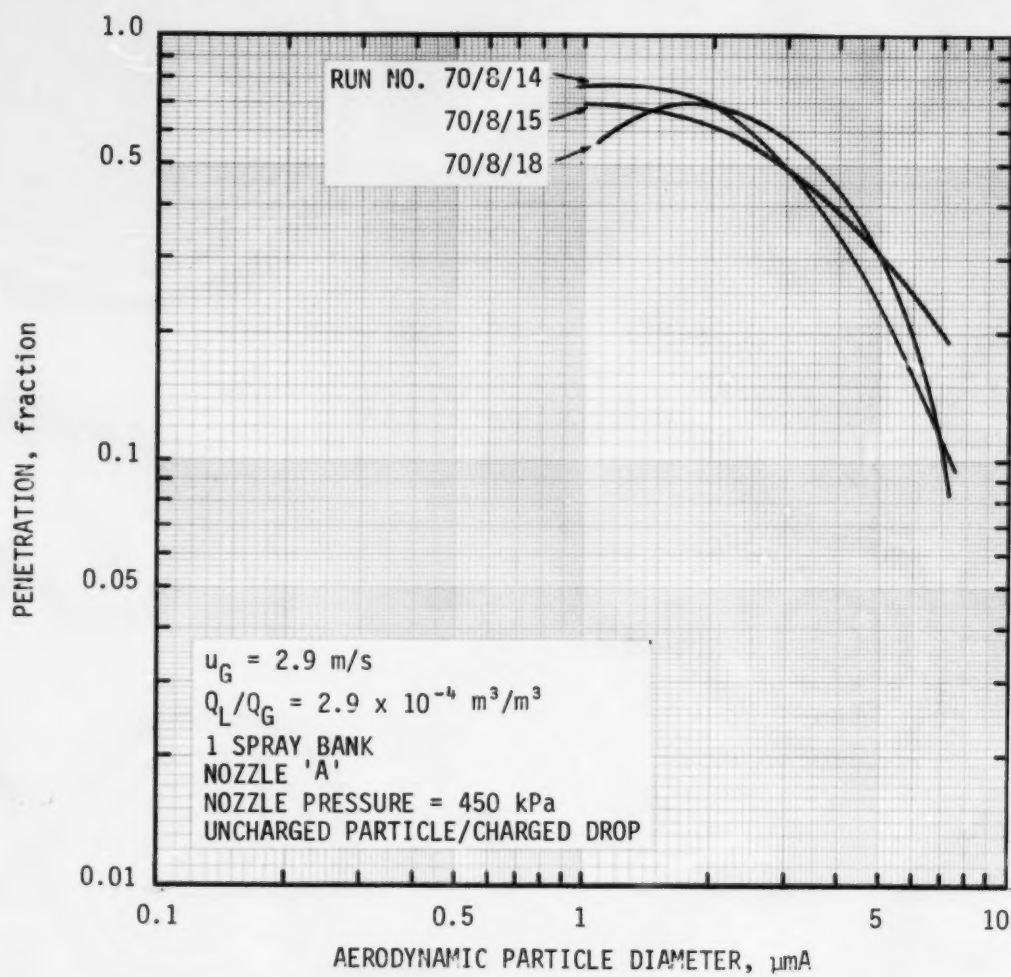


Figure 41. Experimental spray scrubber penetration.

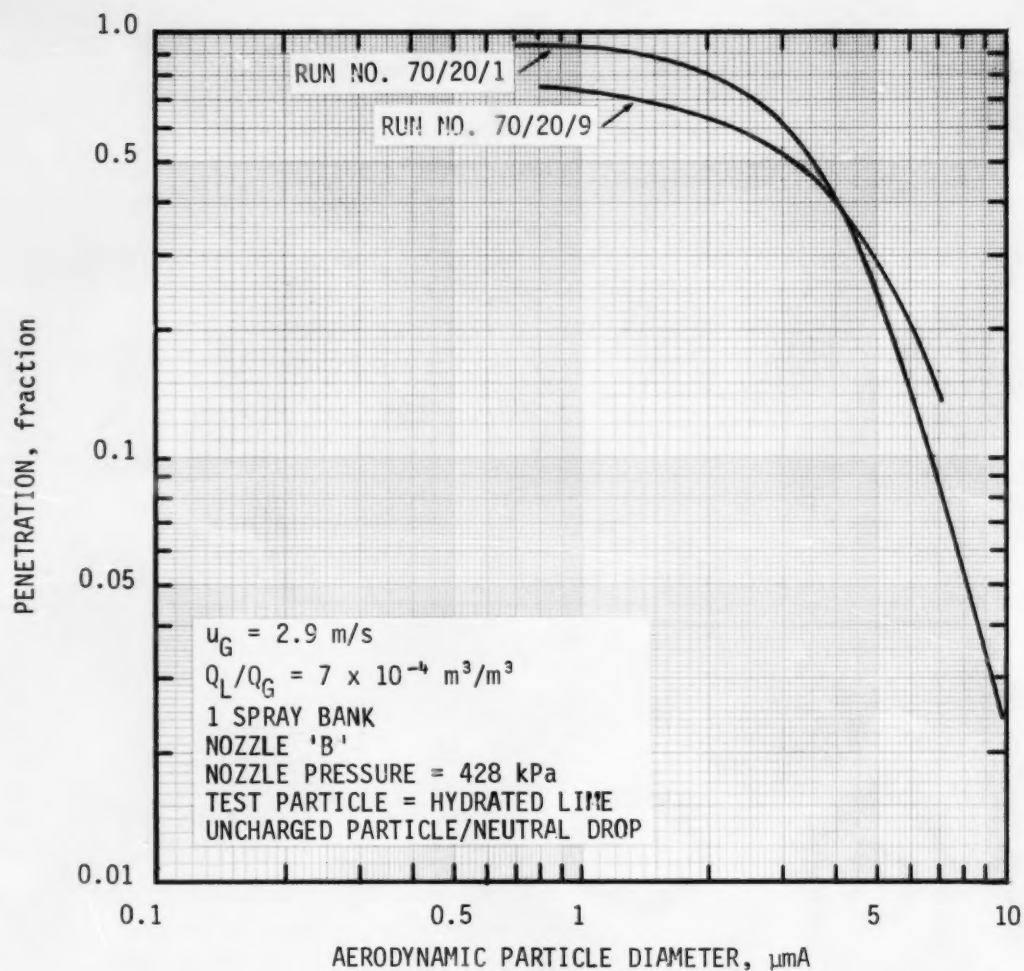


Figure 42. Experimental spray scrubber penetration.

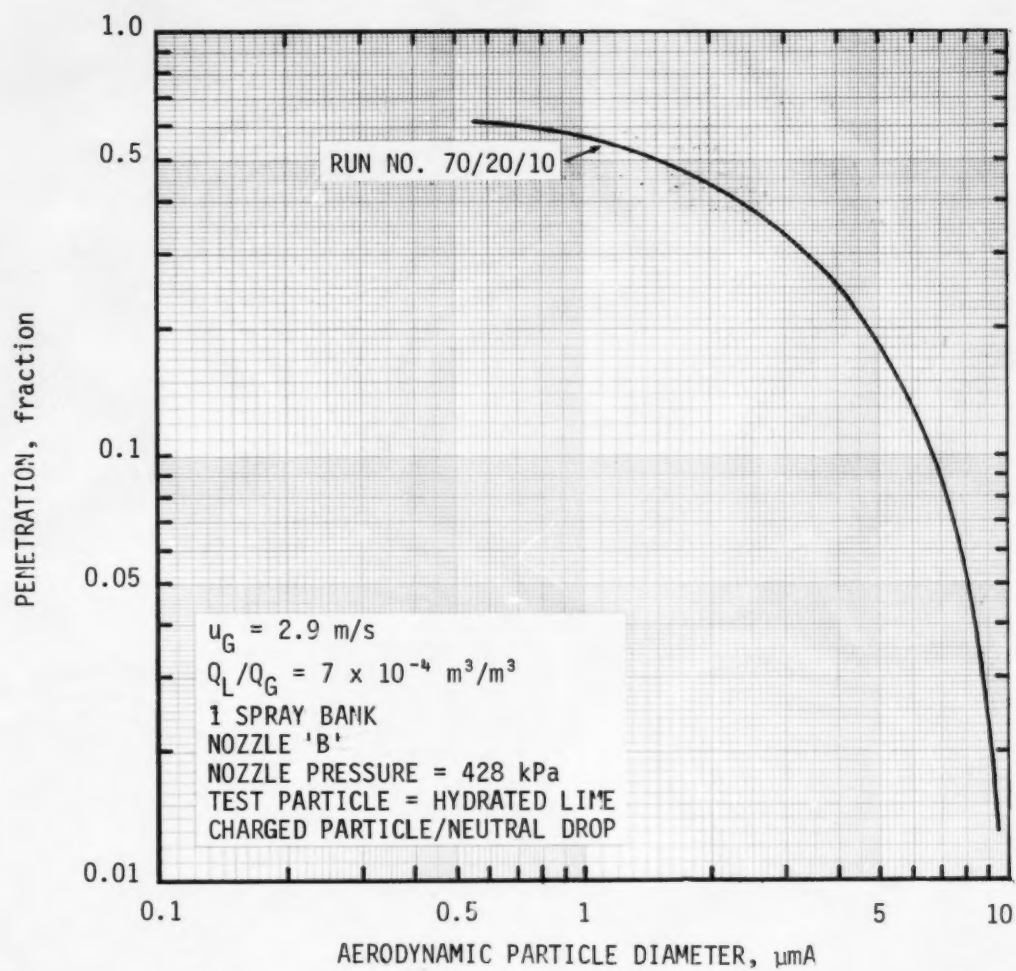


Figure 43. Experimental spray scrubber penetration.

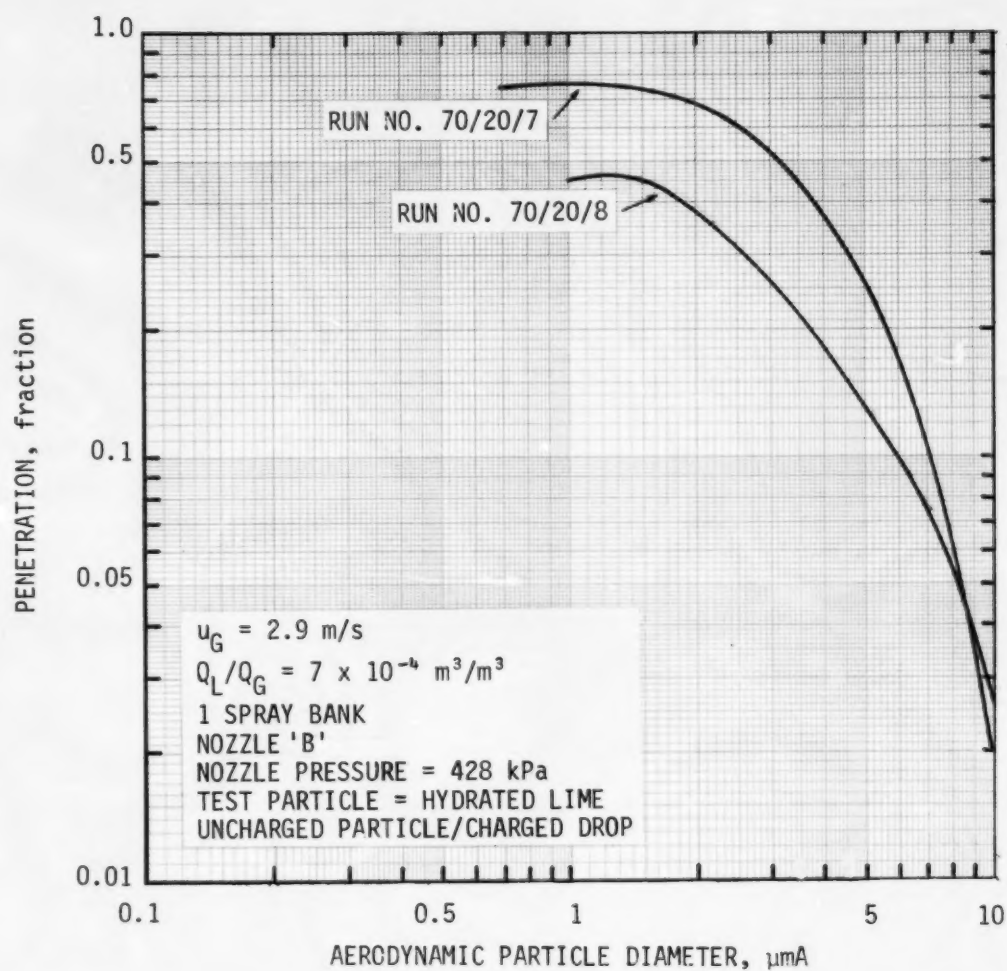


Figure 44. Experimental spray scrubber penetration.

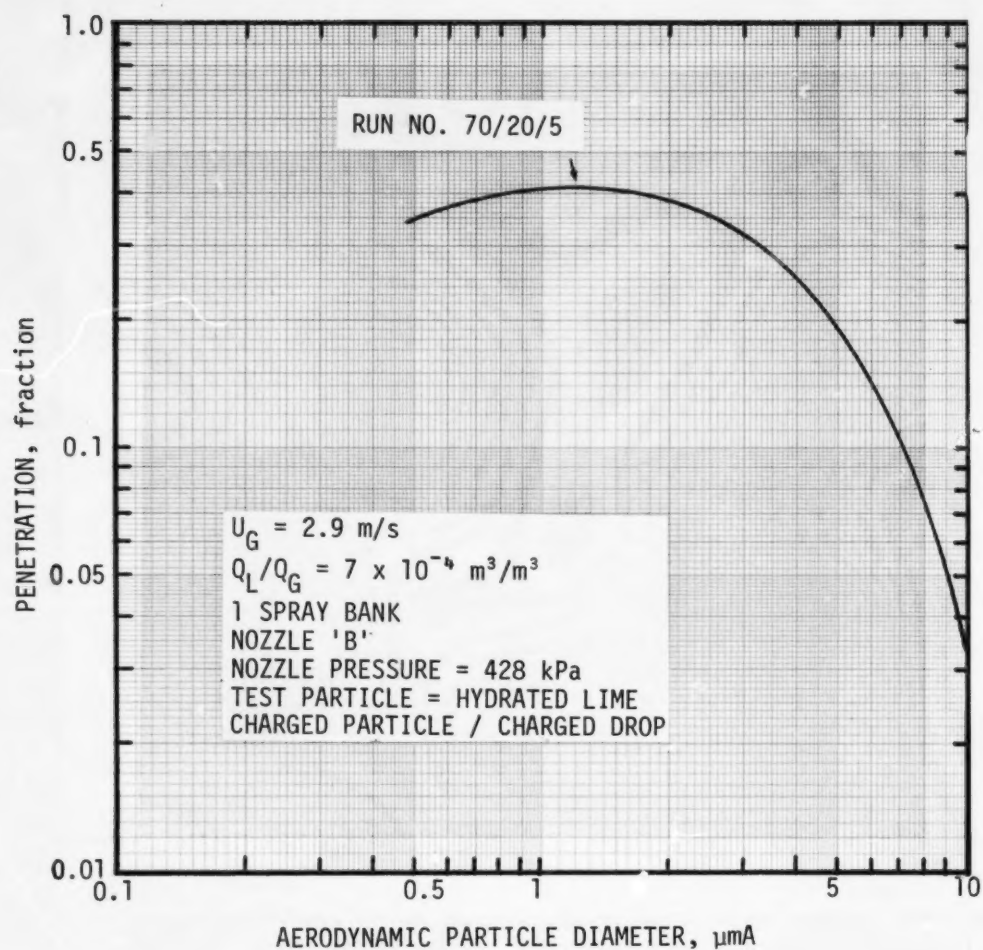


Figure 45. Experimental spray scrubber penetration.

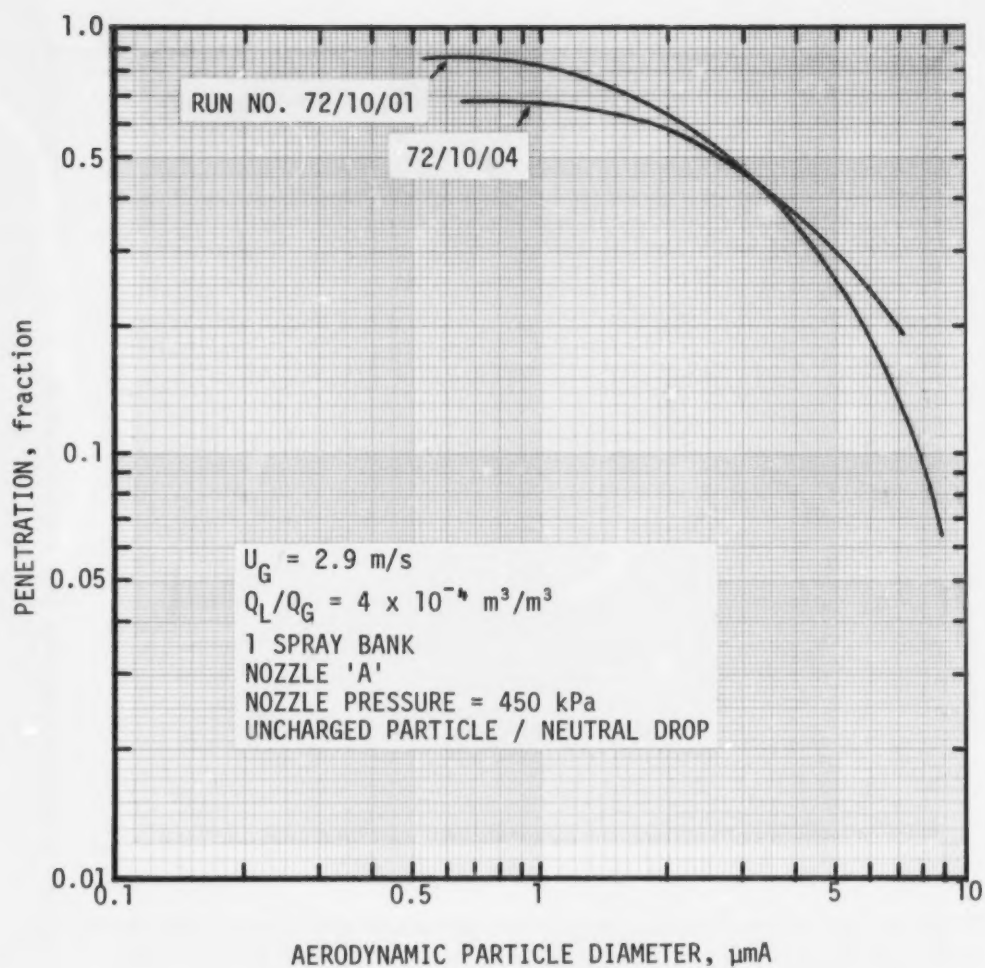


Figure 46. Experimental spray scrubber penetration.

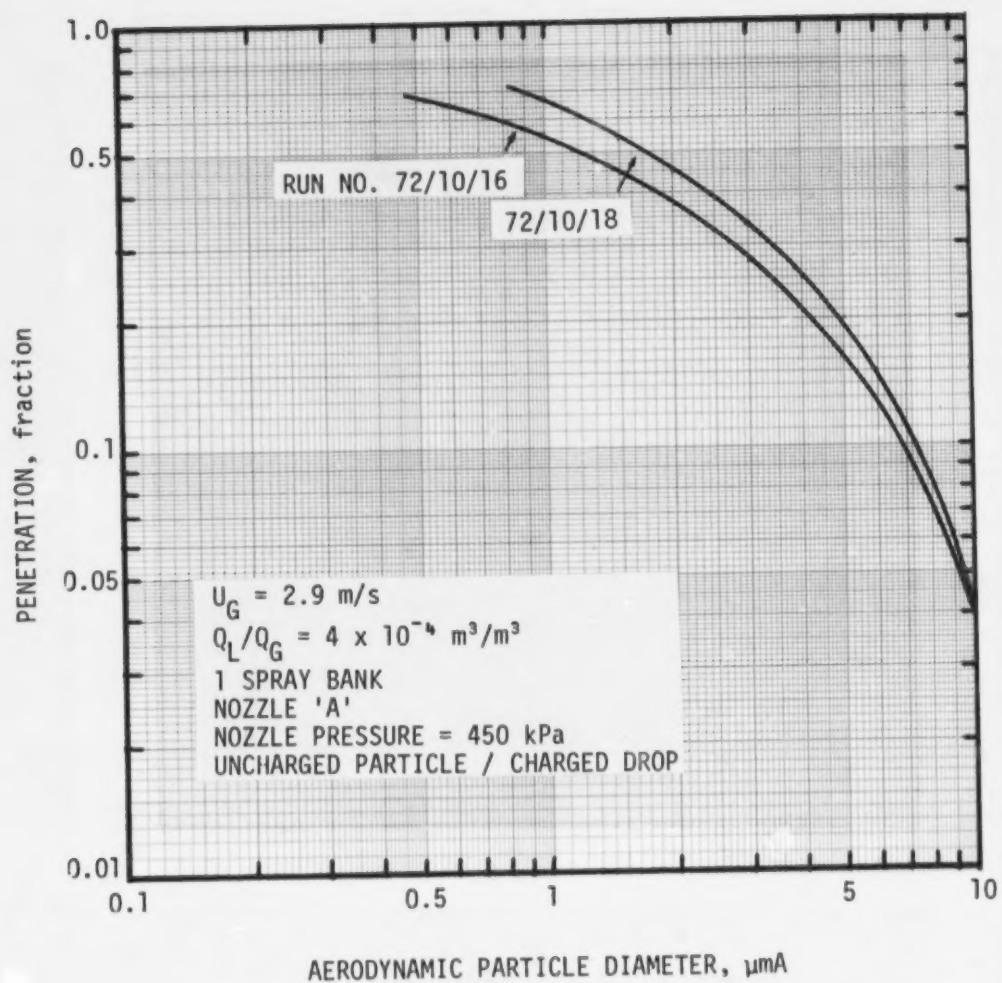


Figure 47. Experimental spray scrubber penetration.

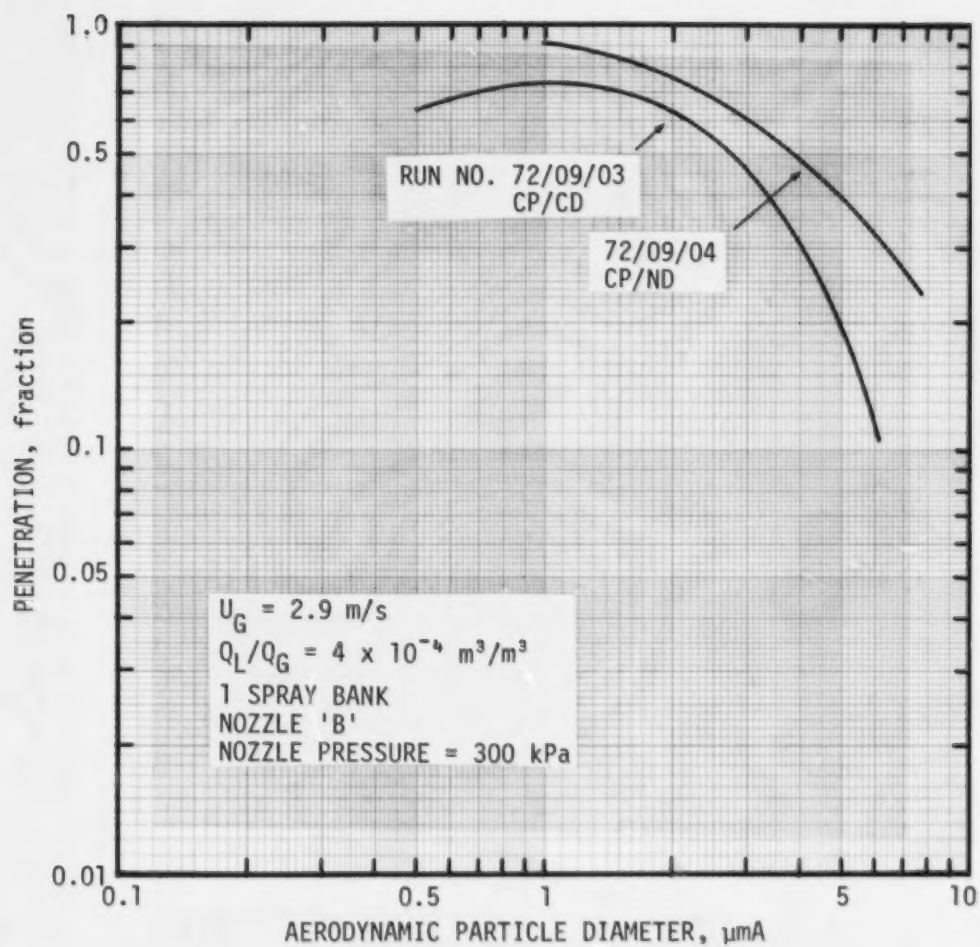


Figure 48. Experimental spray scrubber penetration.

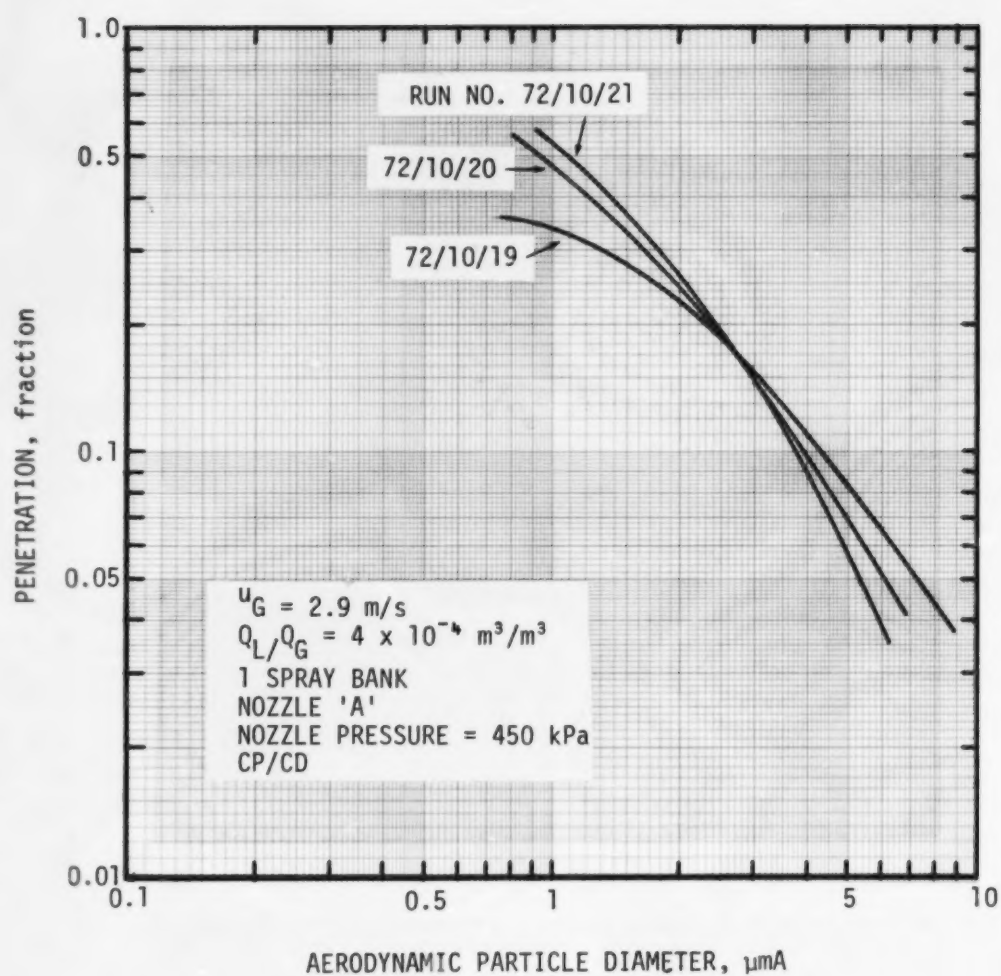


Figure 49. Experimental spray scrubber penetration.

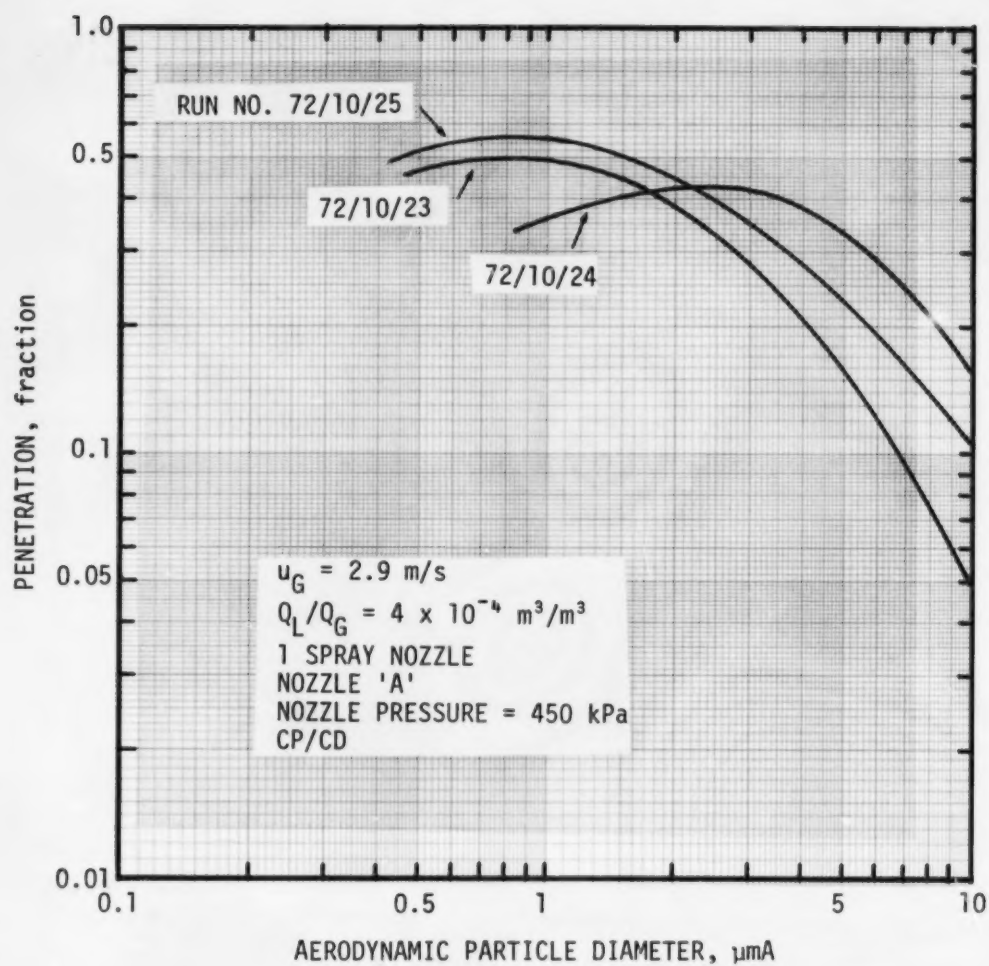


Figure 50. Experimental spray scrubber penetration.

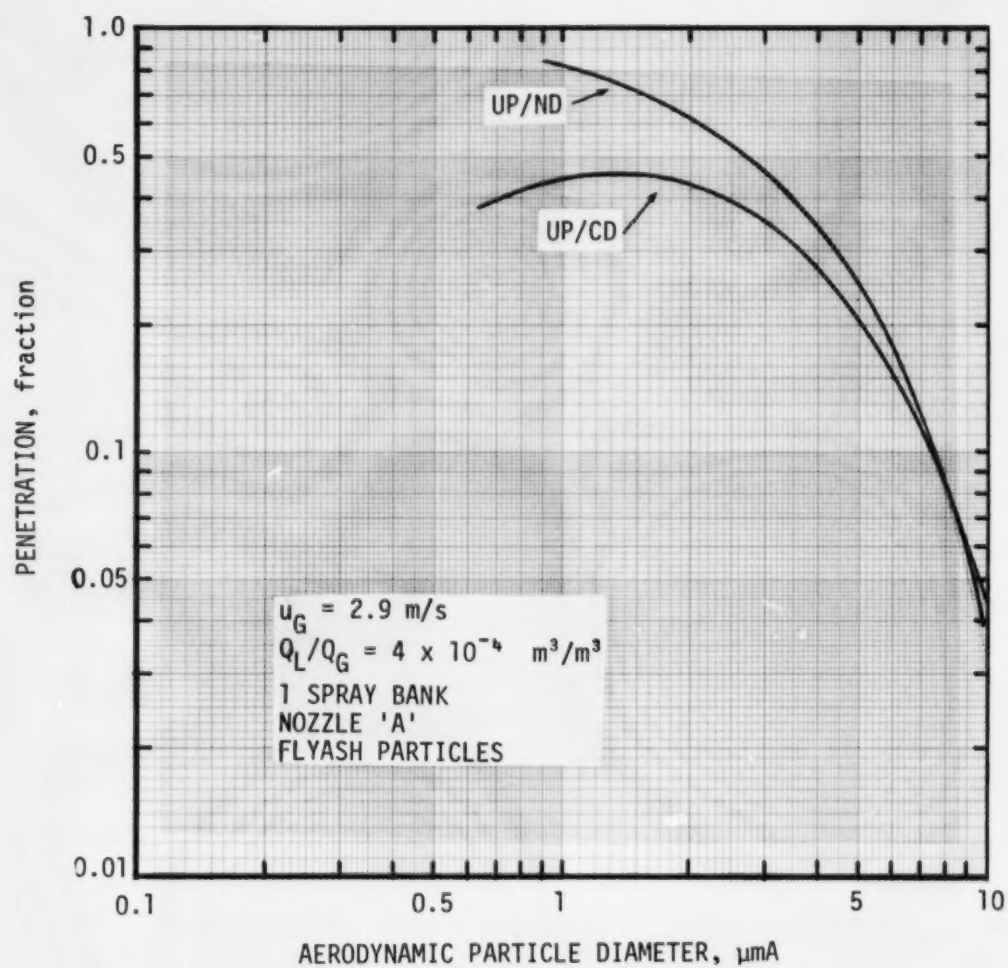


Figure 51. Experimental spray scrubber penetration.

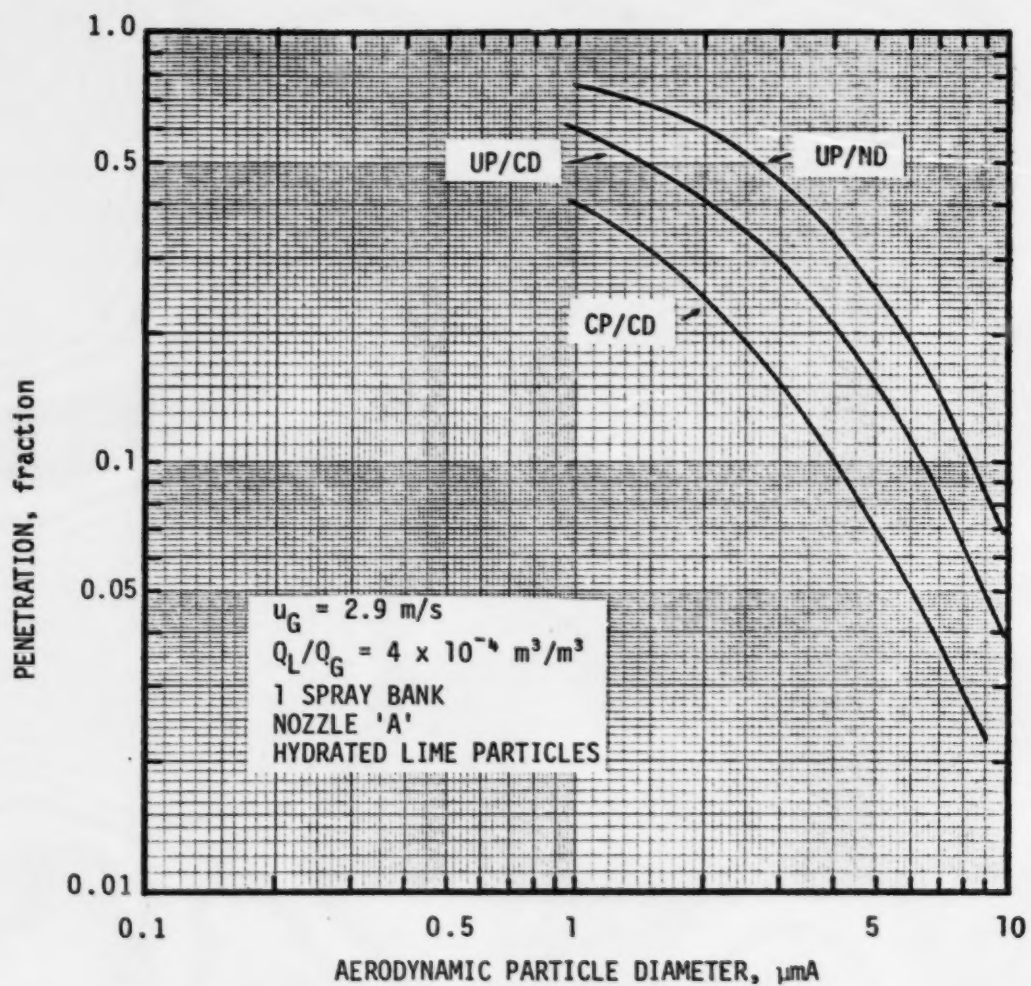


Figure 52. Experimental spray scrubber penetration.

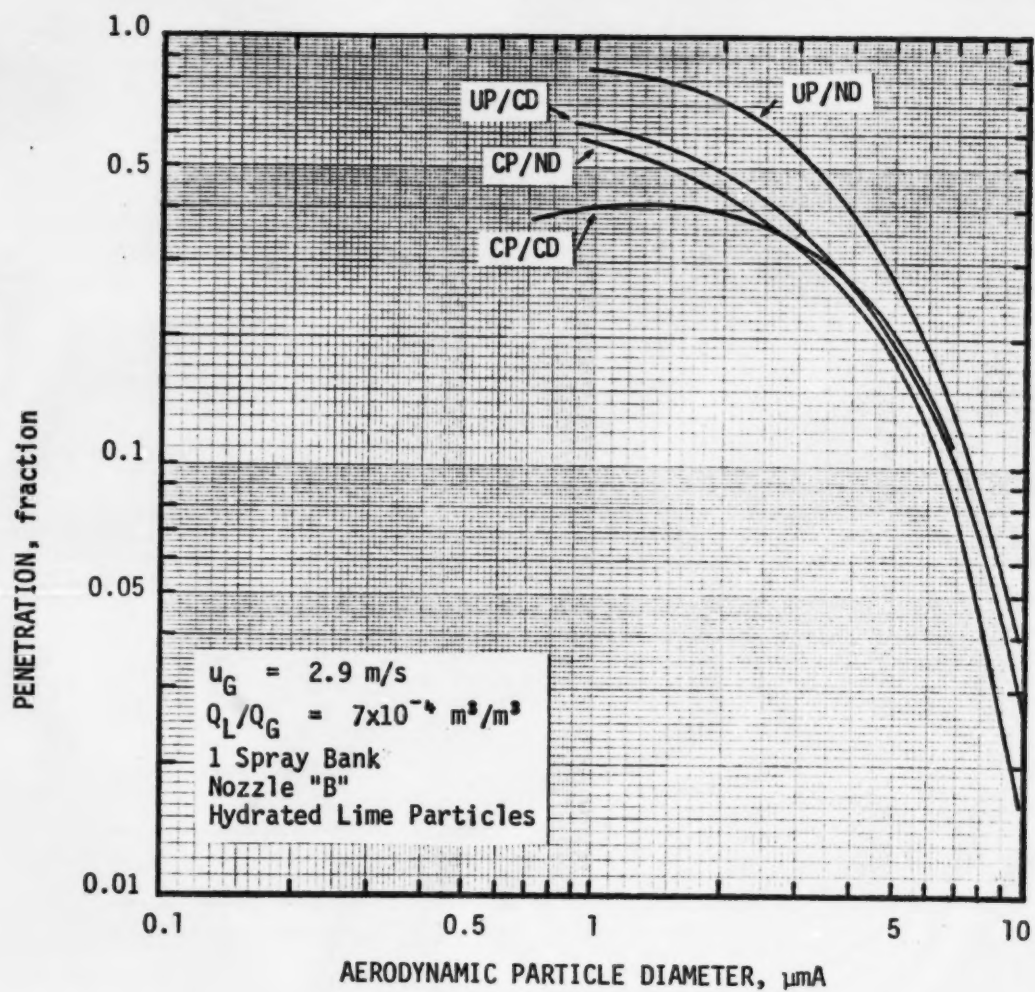


Figure 53. Experimental spray scrubber penetration.

Nozzle "A" gave better collection efficiency than nozzle "B". Possible explanation is that nozzle "A" produces finer drops.

The present particle charger was of wire/rod configuration. The ground electrodes were 1.3 cm diameter aluminum tubing. Although the collection surface and gas residence time in the charging zone were small, the particle charger still collected large amounts of particles as reviewed by Figure 54. The collection by the particle charger should be included in the evaluation of SCAT since an actual SCAT will have a particle charger.

COMPARISON BETWEEN THEORY AND DATA

The SCAT spray scrubber performance was predicted using the appropriate single drop collection efficiency and equations of Calvert et al., (1975) and of Crawford (1976). When using the equation of Calvert, et al., the measured single drop collection efficiency of Walton and Woolcock (1960) was used for ND/UP condition and the theoretical single drop collection efficiency of Nielsen (1974) was used for electrostatically augmented conditions. In using Crawford's equation, the single drop collection efficiency derived by Crawford himself was used.

ND/UP Condition

In calculating the average single drop collection efficiency, one needs to know the drop trajectory. In predictions using the equation of Calvert, et al., drop trajectory equations presented by Giffen and Muraszen (1953) were used because they were used by Walton and Woolcock (1960) in their calculations. The equations are:

1. Turbulent flow, $N_{Red} > 500$

$$u_d = \frac{u_{do}}{1 + \frac{0.33 \rho_G}{d_d \rho_L} u_{do} t} \quad (90)$$

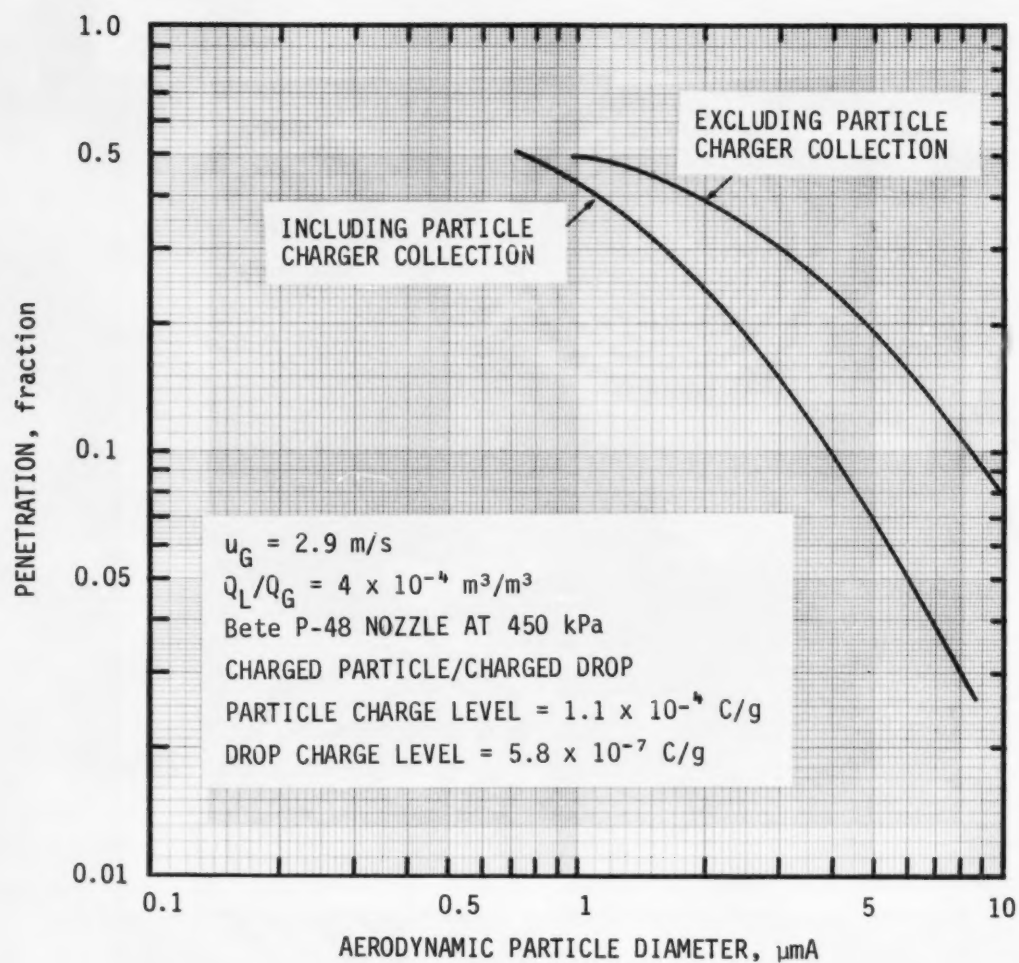


Figure 54. Measured spray scrubber penetration.

$$x_d = \frac{d_d \rho_L}{0.33 \rho_G} \ln \left[1 + \frac{0.33 \rho_G}{d_d \rho_L} u_{do} t \right] \quad (91)$$

2. Transition flow, $500 > N_{Red} > 2$

$$u_d = \frac{1}{\left(\frac{0.01 d_d \rho_G}{\mu_G} \frac{1}{u_{do}} \right) \exp \left(\frac{30 \mu_G t}{d_d^2 \rho_L} \right) - \frac{0.01 d_d \rho_G}{\mu_G}} \quad (92)$$

$$x_d = \frac{d_d \rho_L}{0.3 \rho_G} \ln \left[1 + \frac{0.01 d_d \rho_G}{\mu_G} u_{do} \left(1 - \exp \left(- \left(\frac{30 \mu_G t}{d_d^2 \rho_L} \right) \right) \right) \right] \quad (93)$$

3. Laminar flow, $N_{Red} < 2$

$$u_d = u_{do} \exp \left(\frac{18 \mu_G t}{d_d^2 \rho_L} \right) \quad (94)$$

$$x_d = \frac{d_d^2 \rho_L}{18 \mu_G} \left[1 - \exp \left(- \frac{18 \mu_G t}{d_d^2 \rho_L} \right) \right] \quad (95)$$

where u_d = instantaneous drop velocity, cm
 d_d = drop diameter, cm
 ρ_G = gas density, g/cm³
 μ_G = gas viscosity, Pa s
 u_{do} = initial drop velocity, cm/s
 t = drop travel time, s
 x_d = distance travelled by drop relative to the gas, cm
 N_{Red} = drop Reynolds number, dimensionless.

When predicting the efficiency of the spray scrubber using Crawford's equation, the trajectory equations he presented were used. His trajectory equations are:

$$u_d = \frac{4.18 \mu_G}{d_d \rho_G (1 - B)} + \frac{200 \mu_G}{d_d \rho_G} \left(\frac{B}{1 - B} \right) \quad (96)$$

$$x_d = 1.938 \left(\frac{\rho_L d_d}{\rho_G} \right) \ln \left[\frac{N_{Redo}^2 + 195.6 N_{Redo} - 835.5}{N_{Red}^2 + 195.6 N_{Red} - 835.5} \right] + 1.858 \left(\frac{\rho_L d_d}{\rho_G} \right) \ln \left[\frac{N_{Red} - 4.18}{N_{Red}^2 + 200 N_{Red} - 4.18} \right] \quad (97)$$

$$B = \left(\frac{N_{Redo} - 4.18}{N_{Redo} + 200} \right) \exp \left(- \frac{52.63 \mu_G t}{\rho_L d_d^2} \right) \quad (98)$$

where u_d = instantaneous drop velocity, cm/s
 d_d = drop diameter, cm
 N_{Red} = drop Reynolds number, dimensionless
 N_{Redo} = initial drop Reynolds number, dimensionless
 t = drop travel time, s
 ρ_G = gas density, g/cm³
 ρ_L = liquid density, g/cm³
 μ_G = gas viscosity Pa s
 x_d = distance travelled by drop relative to the gas, cm

Since the drops from a hollow cone nozzle are localized at the edge of the spray cone, the drops are not travelling parallel to the gas stream, but at an angle with the gas stream. Therefore, in calculating the single drop

collection efficiency, the resultant relative velocity between the gas and the drop was used for calculating the inertial impaction parameter.

The spray nozzles in the spray scrubber were equally spaced in the duct. Therefore, some drops travelled a greater distance before striking the wall. The drop range was different for each drop. To simplify the calculations the average drop range of all drops was used. This is equivalent to the assumption that a drop initially located at the center of the scrubber, travelled at an angle with the scrubber axis until it hits the wall.

Figure 55 shows the measured scrubber penetration along with that predicted for UP/ND condition. As can be seen, the predictions from the equation of Calvert et al. correlate better than Crawford's predictions with the data. While Calvert et al. assumed complete particle mixing in deriving their equation, Crawford (1976) assumed no particle mixing. Therefore, if " f_A " in the equation of Calvert et al., were assumed to be 1, then the efficiency predicted from Crawford's equation would be equal to minus the logarithm of the penetration from Calvert, et al.'s equation; i.e.

$$(E)_{\text{Crawford}} = - \left(\ln P_{t_d} \right) \text{ Calvert, et al.} \quad (99)$$

It follows that the efficiency predicted from Crawford's equation will always be higher than that predicted from the equation of Calvert, et al. Figure 54 shows that Crawford's equation predicted a lower efficiency than Calvert, et al.'s equation. The main reasons are that different drop trajectory equations and single drop collection efficiency were used. Crawford's equations for drop trajectory and single drop collection efficiency predict a faster drop deceleration than that of Giffen and Muraszen (1953) and a lower single drop collection efficiency than that measured by Walton and Woolcock. If the single drop efficiency presented by Walton and Woolcock and Giffen and Muraszen's equations for drop trajectory were used with Crawford's equation for predicting scrubber efficiency, the predicted efficiency would be much too high when compared to the data.

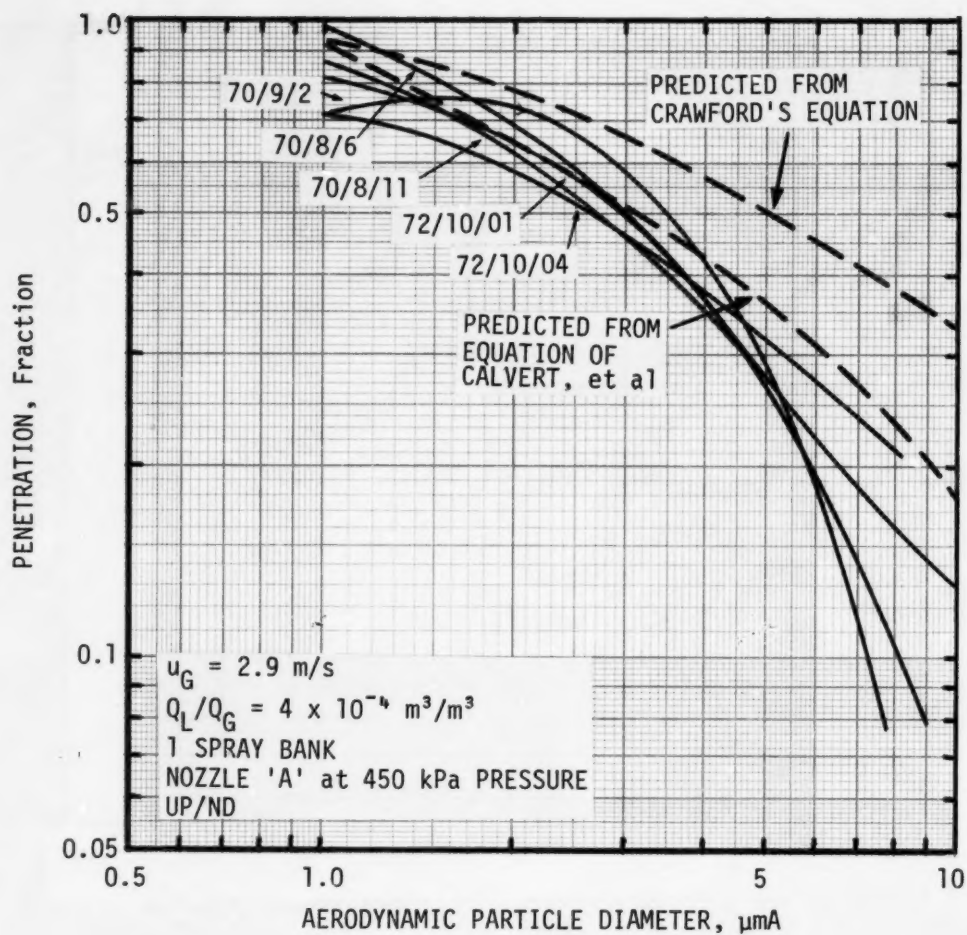


Figure 55. Predicted and measured spray scrubber penetration.

Figure 56 shows the predictions by Calvert, et al.'s equation along with measurements for nozzle "B". Again, the agreement is good.

From these calculations, it seems that the equation of Calvert, et al. will give a reasonable prediction for the spray scrubber performance under ND/UP condition.

Electrostatically Augmented Conditions

The equation of Calvert, et al. and the theoretical single collection efficiency presented by Nielsen (1974) were used for predicting the scrubber penetrations for electrostatically augmented conditions. The average charge on drops was measured to be $+ 5.8 \times 10^{-7}$ c/g. Drop charge as a function of drop diameter was not measured in the present study. It was assumed in the calculations that drops were of the same diameter and each drop carried the same amount of charge.

Charges on particle as a function of particle diameter were also not measured. They were calculated from equations of Pontius, et al. (1977). According to Pontius, et al., the total charge accumulated by a particle of diameter " d_p " in an electric field " E " where there exists an ion density " N " are:

$$N_p = \left(N_p \right)_{\text{field}} + \left(N_p \right)_{\text{diff.}} \quad (100)$$

where

$$\left(N_p \right)_{\text{field}} = \frac{\pi \epsilon_0 d_p^2 E}{e} \left[1 + 2 \frac{\epsilon - 1}{\epsilon + 2} \right] \frac{t}{t + t_0} \quad (101)$$

$$\left(N_p \right)_{\text{diff}} = \frac{2\pi \epsilon_0 d_p kT}{e^2} \ln \left[\frac{d_p e^2 v N t}{8 \epsilon_0 kT} + 1 \right] \quad (102)$$

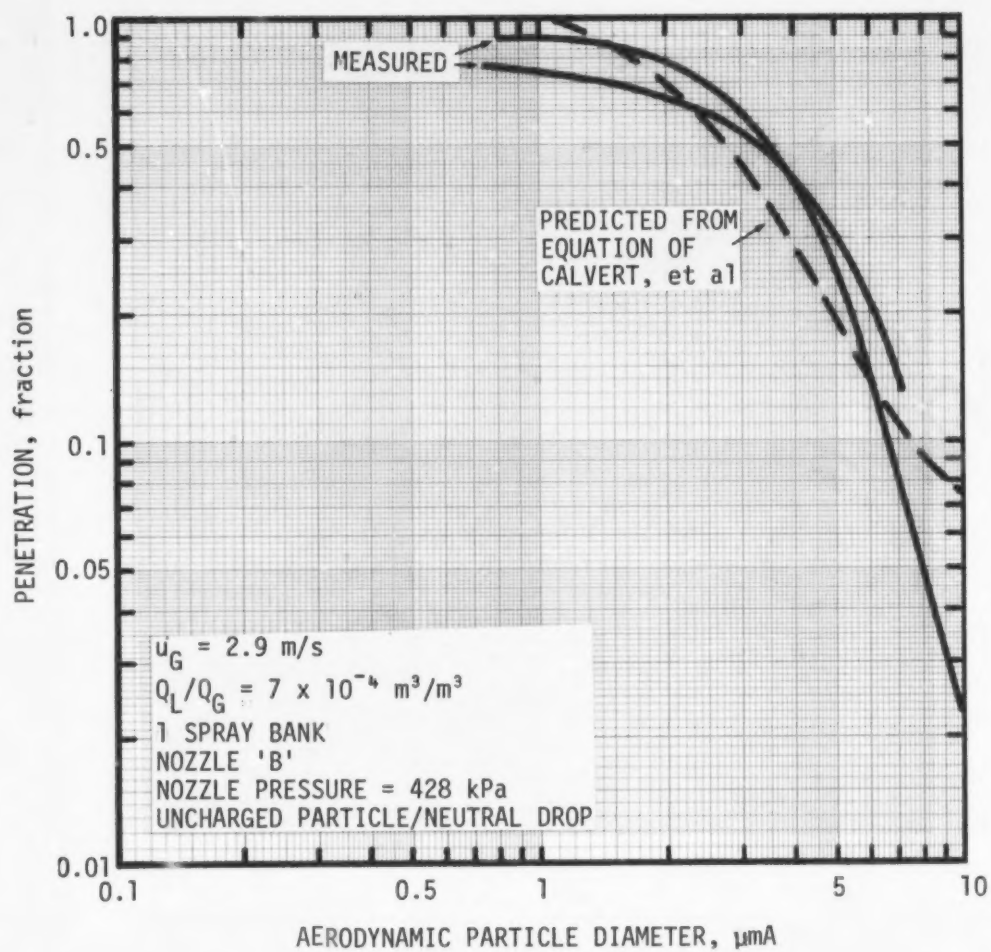


Figure 56. Predicted and measured spray scrubber penetration.

where N_p = total number of elementary charges on particle, number

$(N_p)_{\text{field}}$ = number of charges on particle due to field charging, number

$(N_p)_{\text{diff}}$ = number of charges on particle due to diffusional charging, number

E = field strength, V/m

t = particle residence time, s

$t_0 = \frac{4\epsilon_0}{Ne\mu}$

T = temperature, °K

v = mean thermal ion speed, m/s

N = ion density, /m²

k = Boltzmann's constant, j/°K

e = electron charge = 1.6×10^{-19} C

μ = ion mobility, m²/V-s

ϵ_0 = permittivity of free space, fd/m

ϵ = permittivity of particle, fd/m

The predicted particle charge for the present particle pre-charger with the applied voltage maintained at -10 kV DC is shown in Figure 57. The predicted particle charge level was integrated graphically over the measured particle size distribution to obtain the particle charge/mass ratio. The calculated charge/mass ratio was about 5.7×10^{-7} c/g for $d_{pg} = 3\mu\text{m}$ and $\sigma_g = 2.0$. The calculated charge/mass ratio was about 5 times that measured.

CD/UP and ND/CP Conditions

When either the drops or the particles are charged, the improvement in efficiency is due to image force. According to Figures 26 and 27, the single drop collection efficiency is a function of the initial impaction parameter and the image force parameters. The charged particle image force and charged

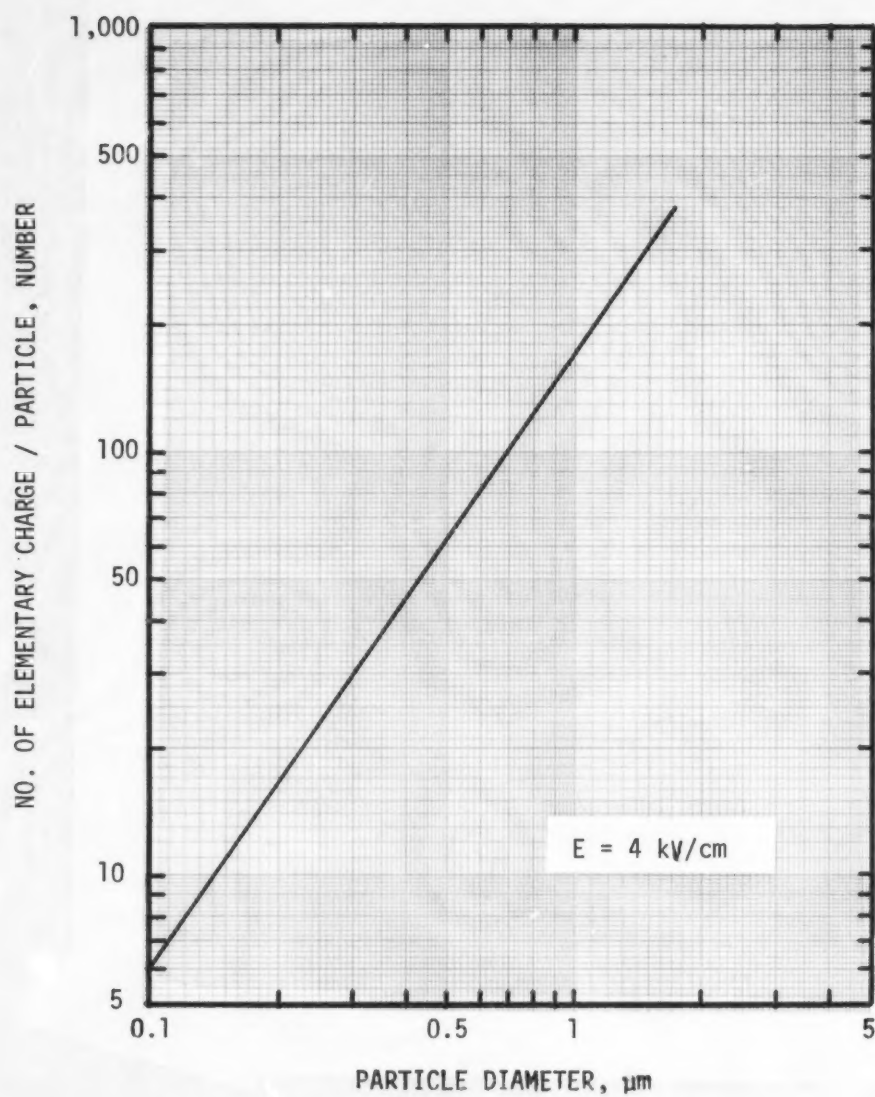


Figure 57. Predicted charge on particle.

drop image force parameter are given in equations 78 and 79. The image force parameters for the scrubber with nozzle "A" are:

$$K_{ic} = 3.45 \times 10^{27} \frac{C' Q_p^2}{u_o d_p} \quad (103)$$

$$\text{and } K_{ip} = 3.68 \times 10^{-3} \frac{d_p^2 C'}{u_o} \quad (104)$$

where K_{ic} = charged particle image force parameter, dimensionless
 K_{ip} = charged collector image force parameter, dimensionless
 C' = Cunningham slip collection factor, dimensionless
 d_p = particle diameter, μm
 Q_p = charge on particle, C
 u_o = relative velocity between drop and gas, cm/s

For $d_{pa} = 1 \mu\text{mA}$, the above equations become

$$K_{ic} = 7.87 \times 10^{-7} / u_o$$

$$K_{ip} = 1.4 \times 10^{-3} / u_o$$

These two force parameters would be very small except at relative velocities close to zero. Since the relative velocity between drop and gas will not be zero in the present scrubber, the imagine forces will be too small to have any effect on scrubber efficiency. This is contrary to experimental findings of the present study and those of others.

CD/CP Condition

When drops and particles are oppositely charged, the Coloumbic attraction force between the particles and drops will increase the collection efficiency. Figures 58 and 59 show the predicted scrubber penetration along with that measured for nozzles "A" and "B", respectively. As can be seen, the agreement is good for particles larger than $3 \mu\text{m}$ in diameter. The theory predicted that there will be minimal improvement in efficiency for particles larger than $5 \mu\text{m}$ in diameter. There is also consistent with experimental findings.

The predicted penetration is higher than measured for particles smaller than $3 \mu\text{m}$ in diameter. One possible explanation for this discrepancy could be due to the use of average drop range. Figure 60 shows a plot of the predicted single drop collection efficiencies for $1 \mu\text{m}$ diameter particles versus drop range. As can be seen, electrostatic augmentation does not have much effect until the drop has been slowed down, i.e. " R_d " is large. In performance predictions, drop range was limited to 70 cm which was the average drop range. In reality, drop range varied between 9 cm and 97 cm - depending on the location of the nozzle. Therefore, the use of average drop range could have underestimated electrostatic effects.

Other possible explanations are that drops are not of uniform diameter and charge level. The present study used induction for drop charging. Not all drops, especially those located at the center of the spray cone, could be charged. This means some drops will carry a higher charge level while some are not charged.

No matter what the actual explanation is, a new or modified theory for predicting the performance of a charged spray scrubber is needed.

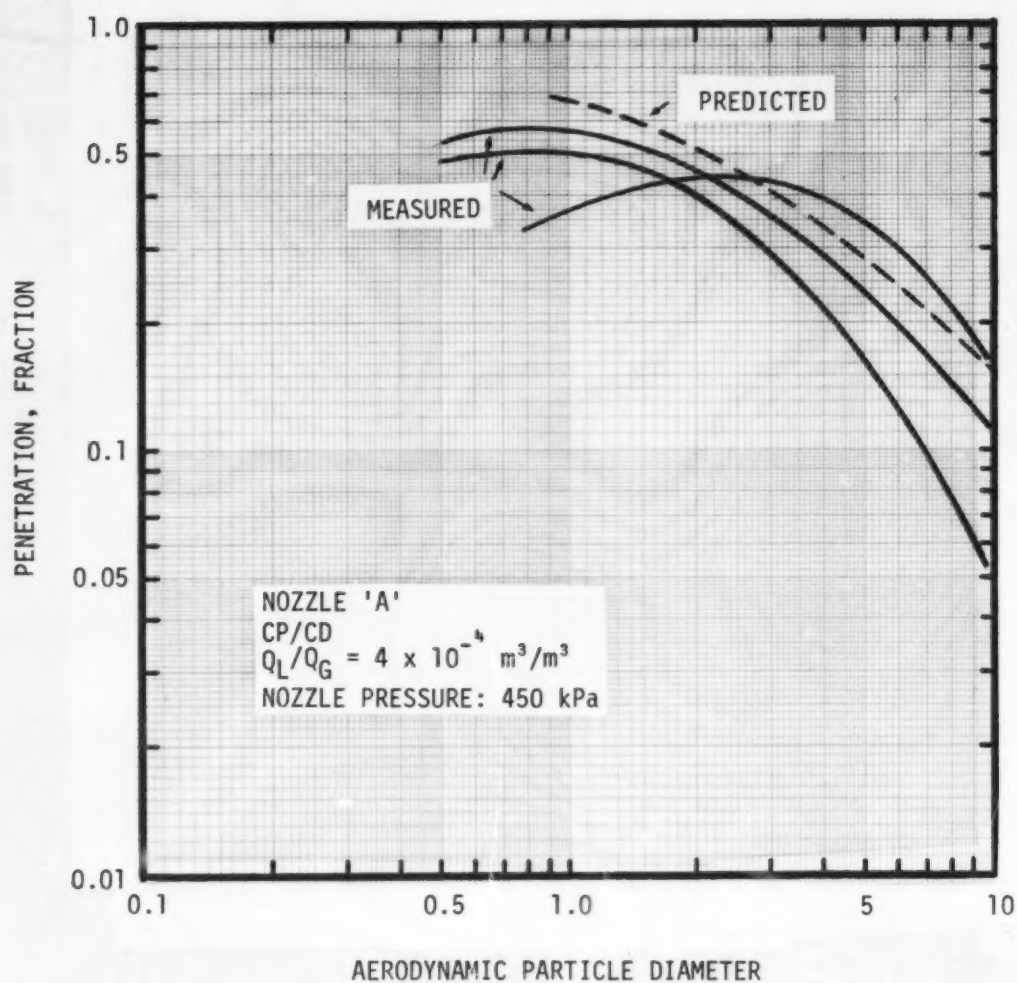


Figure 58. Measured and predicted spray scrubber penetration.

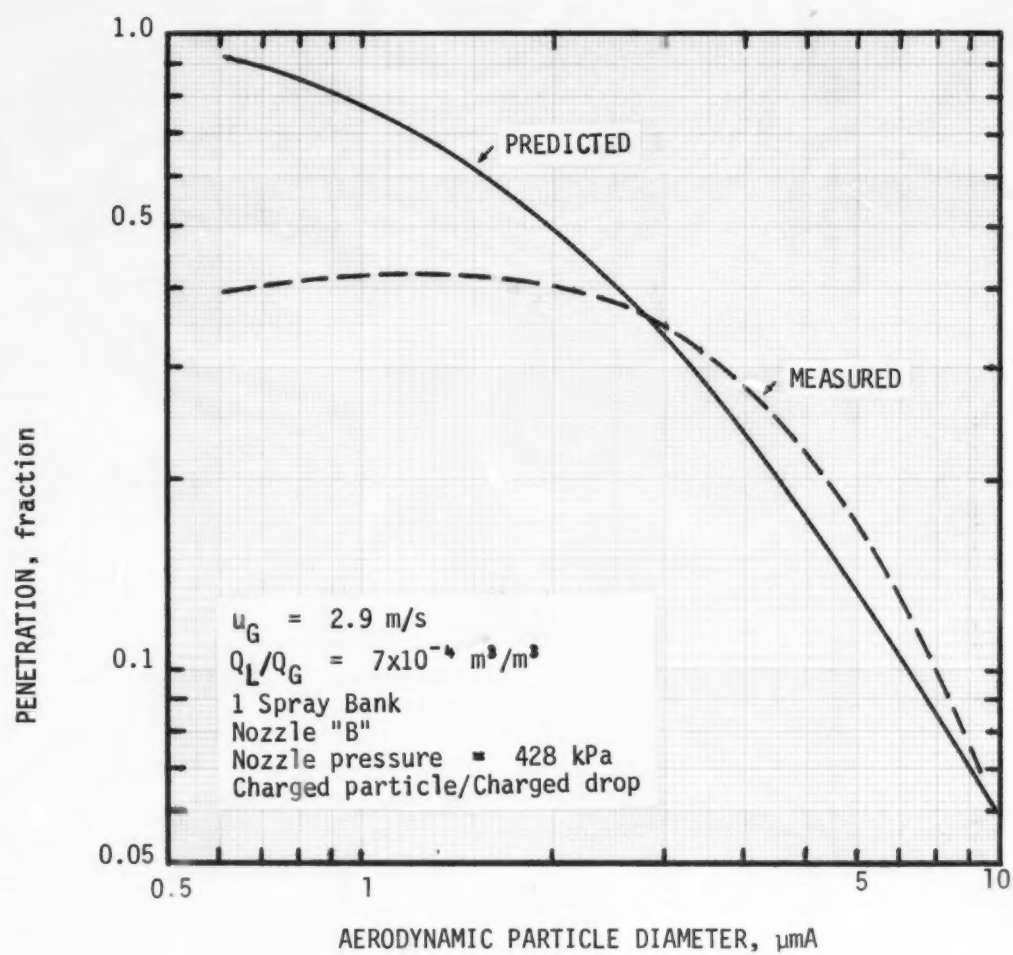


Figure 59. Predicted and measured spray scrubber penetration.

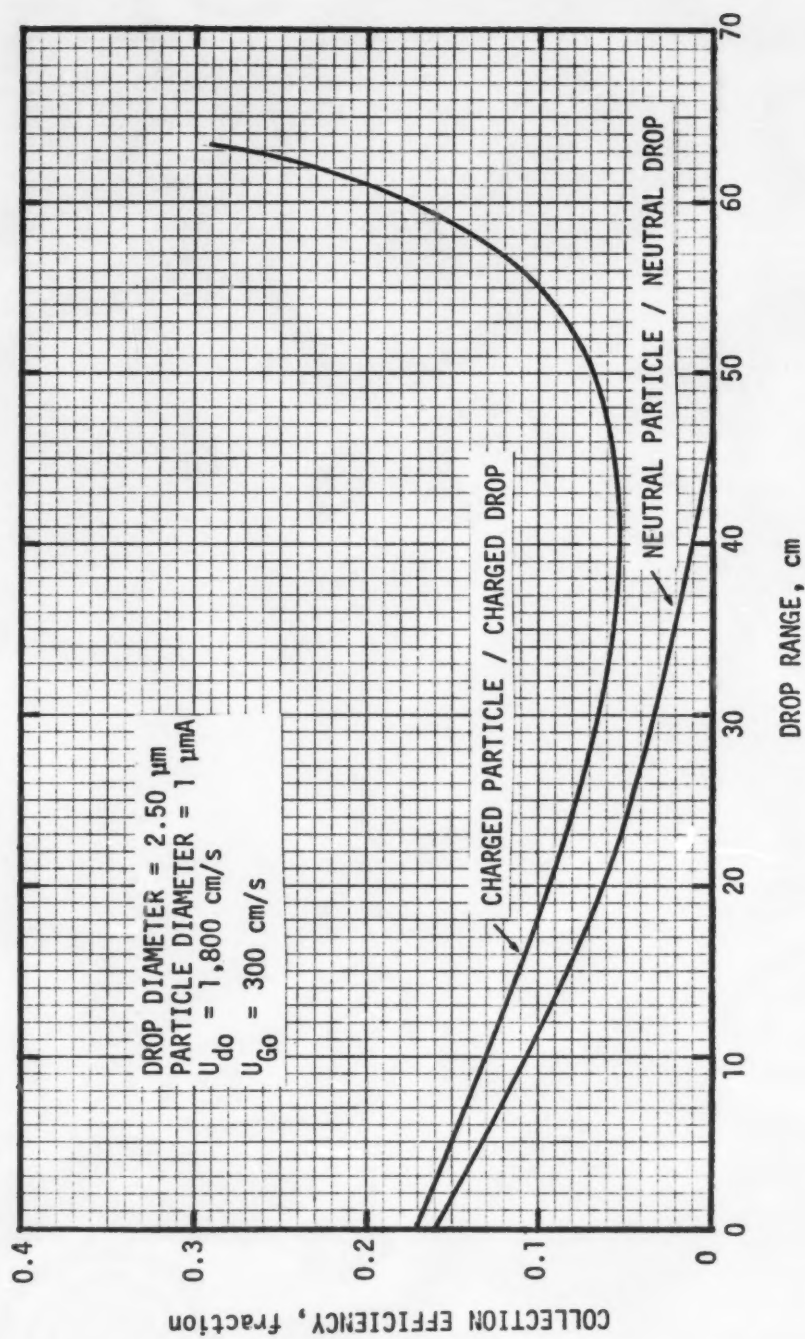


Figure 60. Single drop collection efficiency as a function of drop range.

SECTION 6

PROTOTYPE SCAT STUDY

PROTOTYPE SCAT SYSTEM

A prototype SCAT scrubber system was designed and built. It consisted of two sections. One section housed the charged spray scrubber; the other section had three air curtains and one push jet. Both sections were mounted on casters; therefore, the distance between the air curtains and the spray scrubber could be adjusted.

The spray scrubber (Figure 61) had a cross section of 2.44 m x 1.83 m (8 ft x 6 ft). The bottom 0.61 m (2 ft) was the scrubber sump. Therefore, the active scrubber cross section was 1.83 m x 1.83 m (6 ft x 6 ft). The scrubber was made of aluminum frames and the shell was made of thin polyethylene film. The scrubber sump was made of aluminum plates.

There were 36 large pigtail nozzles at the scrubber front surface. The sprays were concurrent with the gas flow. The capacity of each spray nozzle was 9.8 l/min (2.6 GPM) at a nozzle pressure of 377 kPa (40 psig).

About 1 m downstream from the sprays were four rows of zigzag baffles to remove the water drops.

The scrubber was designed for an air velocity of 4.5 m/s (10 mph). At this velocity the pressure drop for the entrainment separator is approximately 1.3 cm W.C. (0.5 in. W.C.).

At the end of the spray section was a pull fan to overcome the entrainment separator pressure drop and to prevent back flow and eddies forming in the scrubber. We had looked at various options for use as a pull fan. A panel fan would be the most suitable design because it is compact and relatively light weight. For example, a Hartzell 5 HP panel fan, rated at 14.9 m³/s

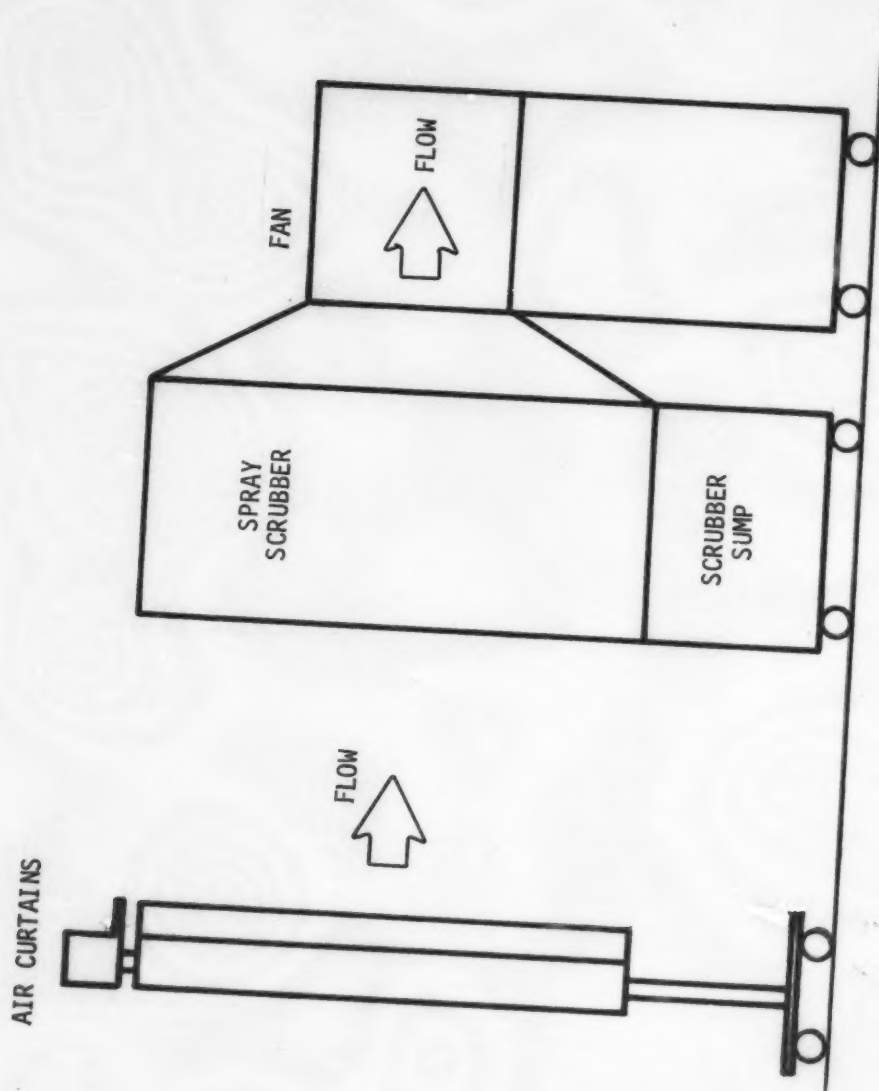


Figure 61. Prototype SCAT system.

(31,500 CFM) at 1.3 cm W.C., weights 245 kg (540 lbs). Its 1.8 m x 1.8 m (6 ft x 6 ft) from could be mounted directly to the scrubber, making a transition unnecessary.

However, a 15 HP vane-axial fan, rated at $16.0 \text{ m}^3/\text{s}$ (34,800 CFM) at 1.3 cm W.C., was in our inventory and was used rather than purchasing a panel fan. The vane-axial fan was mounted on a frame separate from the scrubber and then rolled into position behind the SCAT. A conical transition duct was used to connect the scrubber and fan.

The other section had three independent air curtains and a propeller fan. The design of the air curtain was the same as that described in Section 4. Two of the air curtains were mounted vertically and about 1.83 m (6 ft) apart. The other curtain was mounted horizontally 2.7 m (9 ft) above the ground. The air curtains could be swivelled as needed to deflect crosswinds and buoyant smoke plumes. The propeller fan, which was 86 cm in diameter and rated at $5.4 \text{ m}^3/\text{s}$ at 1.3 cm W.C., was located at the center. The fan was for producing a "push jet".

Even though the air curtain section had three air curtains and one air push jet, they need not be operated simultaneously. Under calm conditions, it could be that only the air push jet is required to move the dust into the spray scrubber. When there is cross wind, one air curtain may be enough to deflect the wind and to convey the dust into the scrubber.

The prototype SCAT scrubber was used in cross wind and hot source experiments. The collection efficiency of the spray scrubber was not measured for those experiments. Therefore, the particle charger and drop charger was not built into the scrubber.

CROSSWIND EXPERIMENTS

Under windy conditions, one can put the spray scrubber of the SCAT system downwind of the fugitive particle source. The wind will carry the particles to the spray scrubber where particles are removed from the air by water

sprays. If the spray scrubber cannot be put downwind, the wind can be deflected from the FPE source with wind screens or air curtains. Fugitive particles are then conveyed to the spray scrubber with push jets. It is also possible that one air curtain is used to both deflect the wind and convey the particles.

Literature Search

When the SCAT air curtain air flow is blown at an angle against the oncoming wind, the wind will be partially or completely deflected. For the SCAT system, complete wind deflection is required to avoid disturbances in FPE containment. Complete wind deflection is defined as the condition for which the resultant air flow of crosswind and SCAT air curtain jet flow bypassed the SCAT scrubber system. Complete wind deflection occurs when the blocking distance or range produced by the SCAT air curtain is larger than the distance between the air curtain and the spray scrubber. This is illustrated in Figure 62.

In order for the air curtain to deflect the wind, the momentum of the air curtain flow in the direction opposing the wind must be equal to or greater than the momentum of the wind, i.e.

$$-w u_{Gj}^2 \cos \alpha_0 \geq L u_a^2 \quad (105)$$

Where w = air curtain slot width, cm

u_{Gj} = air exit velocity at air curtain nozzle, cm/s

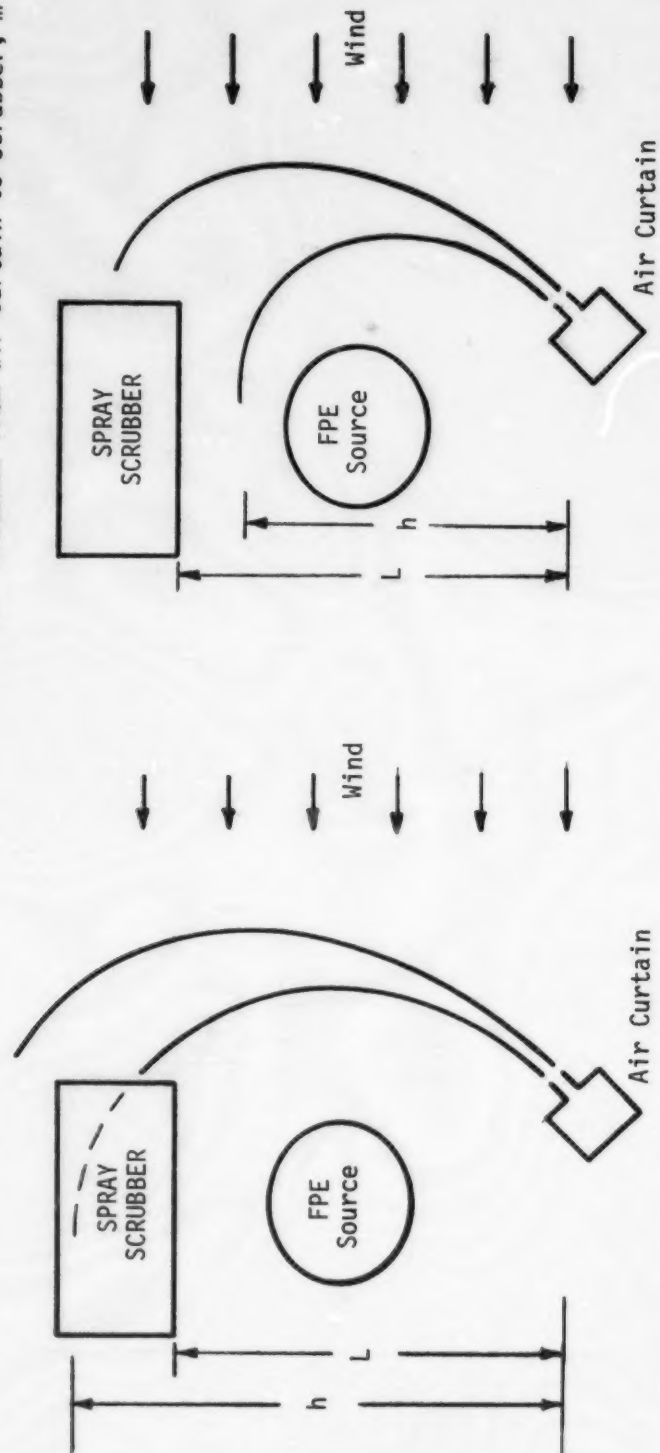
α_0 = incident angle, degree

L = air curtain and spray scrubber separation, cm

u_a = wind velocity, cm/s

The incident angle at which the air curtain meets the wind is the most important parameter. For maximum deflection and resulting blocking distance, $\alpha_0 = 135^\circ$ (45° opposing the wind).

h = blocking distance, m
 L = distance from air curtain to scrubber, m



(a) $h > L$, Crosswind is deflected,

(b) $h < L$, Crosswind is not deflected.

Figure 62. Diagram illustrating wind deflection.

Equation (105) is an over-simplification of the actual situations since jet trajectory is not considered. However, it has been successfully used in designing air curtains for doorways (Baturin, 1972) and in mining applications (Grassmuck, 1969).

In operating the SCAT system in the presence of crosswind, the trajectory of the air curtain jet flow is an important parameter which needs to be considered since it dictates the placement of the air curtain and the spray scrubber. The trajectory of the jet flow has been studied, both experimentally and theoretically, by numerous investigators, e.g. Callaghan and Ruggeri (1948), Kamotani and Greber (1971), Patankar, et al. (1977), Sucec and Bowley (1976), and Mahihata and Miyai (1979).

The trajectory of the jet flow can be predicted by combining the momentum integral equation, which takes into account entrainment and buoyant flux (if the jet flow is hot as in a plume), with the law of conservation of momentum along the trajectory. The resulting equations are very complex and can only be solved by computers. In the following sections, simple approximate equations for estimating jet trajectory are presented. Complicated trajectory formulas will not be given since they require a computer and are unnecessarily complicated for this analysis.

A diagram of a curved jet with basic notation is shown in Figure 63. The trajectory equations presented below will refer to the geometry and notations shown in this figure.

Abramovich (1963) derived an equation for the axis of a jet by geometrically superimposing the flow of surrounding fluid on the jet, i.e. by adding the cross flow velocity vector to the velocity vector of the jet in a given section. His equation for calculating the axis of the air curtain is:

$$\frac{2x}{w} = \frac{2y}{w} \cot \alpha_0 + \frac{0.39}{C_2^{0.5} (\sin \alpha_0)^{1.5}} \left(\frac{2y}{w} \right)^{1.5} \left(\frac{u_a}{u_{Gj}} \right) \quad (106)$$

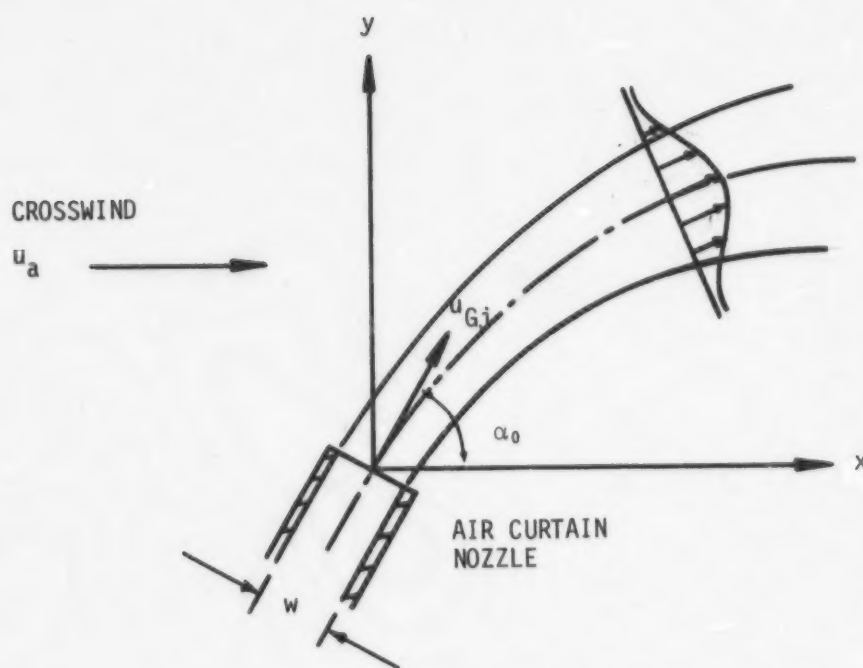


Figure 63. Diagram of a curved jet with the basic notation.

Where u_a = crosswind velocity, cm/s
 u_{Gj} = air curtain nozzle exit velocity, cm/s
 w = slot width, cm
 C_2 = constant, dimensionless
 x = coordinate in the direction of wind, cm
 y = coordinate perpendicular to wind direction, cm
 α_0 = initial incident angle, degree

For a uniform velocity field in region "I" of the jet, $C_2 = 1$. By comparing equation (106) with experimental data of Baturin, Abramovich recommended a value of 0.78 for " C_2 ".

Volinsky (Abramovich, 1963) used a different approach. He derived the equation for finding the curvature of the jet axis by balancing the force caused by the pressure difference at the forward and back surfaces of the jet with the centrifugal force. His axis equation for a plane jet with constant wind velocity and density is:

$$y = \frac{2}{C_3} \left[\pm (C_3 x + \cot^2 \alpha_0)^{0.5} - \cot \alpha_0 \right] \quad (107)$$

$$\text{where } C_3 = \frac{C_4 \rho_a u_a^2}{w \rho_{Gj} u_{Gj}^2 \sin^2 \alpha_0} \quad (108)$$

Where u_a = wind velocity, cm/s
 u_{Gj} = air curtain nozzle exit velocity, cm/s
 C_4 = constant, dimensionless
 x = coordinate in the direction of wind, cm
 y = coordinate in the direction perpendicular to wind, cm/s
 α_0 = initial incident angle, degree
 ρ_a = density of wind, g/cm³
 ρ_{Gj} = density of air at air curtain nozzle exit, g/cm³

The constant " C_4 " is determined from a comparison of the theoretical curve with experimental data. It ranges from 1 to 3.

Both roots of equation (107) have real meaning. The minus sign in front of the first term on the right side gives a second solution, real for $\alpha_0 > \pi/2$.

Shepelev (Baturin, 1972) constructed a picture of the flow by means of an analytical solution, which consisted of adding the stream functions of the jet and the external flow. However, Shepelev's equation gave an exaggerated jet axis curvature (Abramovich, 1963).

It should be mentioned that the above equations can be considered as only rough approximations since the methods used in deriving them are valid only for ideal fluids. For flows of nonideal fluids, the methods may not be accurate.

Empirical equations for predicting the trajectory of the jet axis are also reported in the literature. The empirical equation of Ivanov (Abramovich, 1963) is:

$$\frac{x}{2w} = \left(\frac{\rho_a u_a^2}{\rho_{Gj} u_{Gj}^2} \right)^{1.3} \left(\frac{y}{2w} \right)^3 + \frac{y}{2w} \cot \alpha_0 \quad (109)$$

Where u_a = wind velocity, cm/s
 u_{Gj} = air curtain nozzle exit velocity, cm/s
 w = slot width, cm
 x = coordinate in the direction of wind, cm
 y = coordinate in the direction perpendicular to the wind, cm
 α_0 = initial incident angle, degree
 ρ_a = density of wind, g/cm³
 ρ_{Gj} = density of air at air curtain nozzle, g/cm³

Experiments

The jet axis trajectory equations presented above can be applied to locate the air curtain and the spray scrubber. If an air curtain is solely for

deflecting the crosswind, then the separation between the air curtain and spray scrubber must be smaller than the blocking distance or range of the air curtain. If the air curtain is both for deflecting the wind and conveying the fugitive dust, then the spray scrubber should be located in the path of the jet stream.

Experiments were done to verify the jet trajectory equations. Two types of experiments were performed. In the first series of experiments, efforts were directed at verifying the equations for predicting the maximum range of the air curtain. In the second series of experiments, the actual trajectory of the air curtain was measured.

Air Curtain Range

Experiments were carried out both indoors and outdoors to determine the range of an air curtain.

Indoor Experiments - Figure 64 shows a top view of the equipment layout. For better control of experimental variables, the experiments were done indoors.

An air curtain was used to produce air flow to simulate the crosswind. The wind speed can be adjusted by varying the distance between the air curtain and the SCAT or by opening the fan damper. The wind direction can be varied by rotating the air curtain distribution duct. In the present studies, the wind direction was normal to gas flow direction in the SCAT.

Wind deflection was detected with flags made from aluminum foil. The flags hang down when there is no air movement. Three rows of seven flags each were hung on a vertical plane and at heights of 1.4 m (4.5 ft), 2.3 m (7.5 ft) and 2.7 m (9 ft) from floor. The flag plane coincided with the SCAT center plane.

Instead of fixing the incident angle at 135° , which gives the maximum range, and varying the distance between the air curtain and the spray scrub-

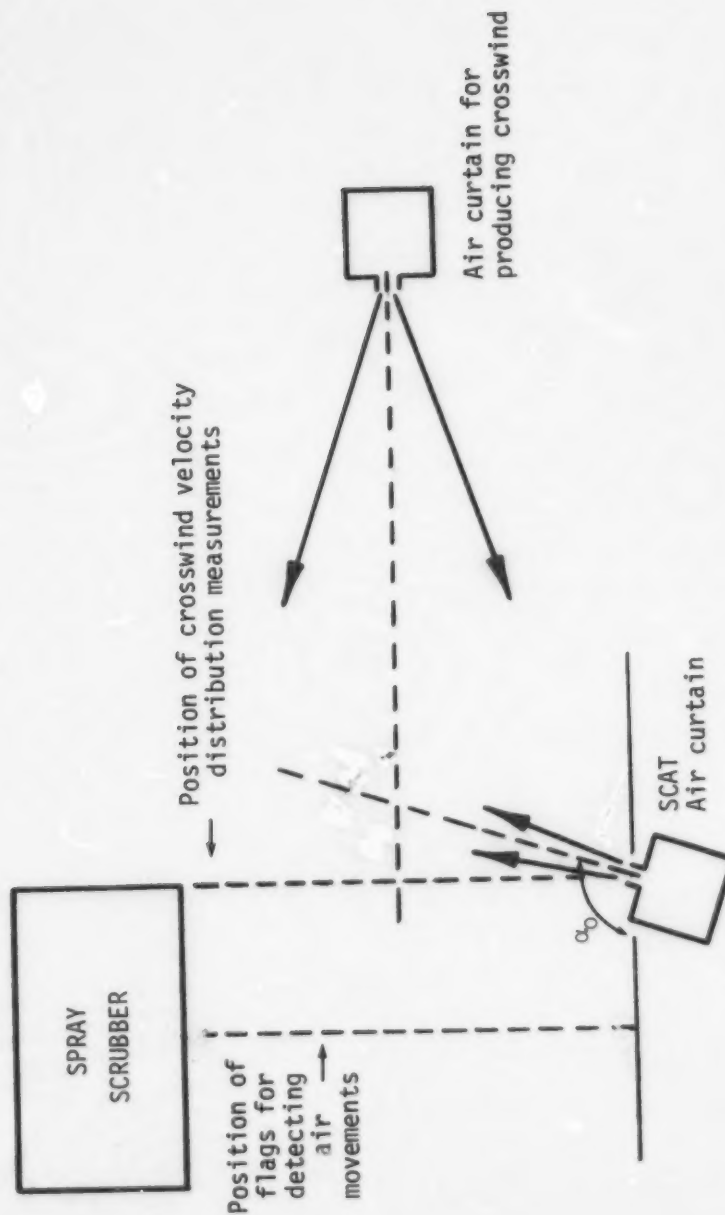


Figure 64. Top view of crosswind experiment equipment arrangement.

ber, experiments were performed by fixing the range and varying the incident angle.

In starting the experiment, the spray scrubber and SCAT air curtain were rolled into position and the distance between the two was adjusted to a desired separation. The slot width of the air curtain nozzles was also adjusted. The fan for the SCAT air curtain was then turned on and the nozzle exit velocity was measured and adjusted to the desired velocity by opening or closing the fan damper. Next, the SCAT air curtain fan was turned off and the crosswind air curtain fan was turned on. The wind velocity was adjusted to the desired value. The SCAT air curtain fan was then turned on again. Starting from an incident angle of 90° , the SCAT air curtain was gradually rotated to increase the incident angle until no movement of the aluminum flags could be observed. The angle at which the flag movement stopped was measured and recorded.

For some experimental conditions, even though the wind could not be completely deflected, it was near complete deflection since there were slight movements for a few flags. For this situation, the incident angle at which there was minimum flag movement was measured and recorded.

The experimental data for a spray scrubber and air curtain separation of 3.1 m (10 ft) are presented in Table 9. Since the crosswind was produced by an air curtain, it did not have a uniform velocity distribution. The wind velocity distribution was measured with a hot wire anemometer (Kurtz velocity meter) at the SCAT air curtain plane. Some typical wind velocity distributions are plotted in Figure 65. The centerline wind velocity and the calculated average wind velocity are tabulated in Table 9.

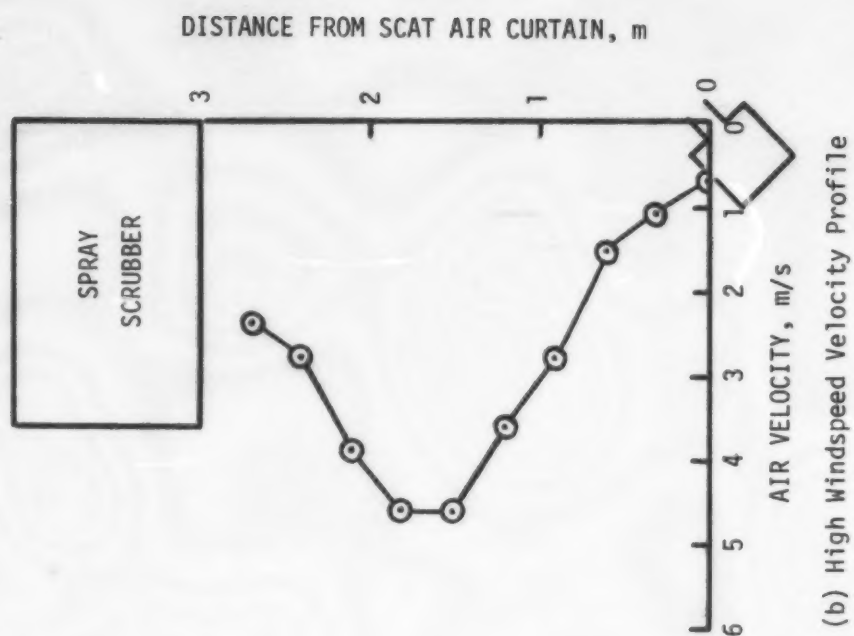
The incident angle at which maximum partial deflection occurred was about 135° . This is expected since this angle produces the maximum range.

Comparison Between Theory and Data - The range of the air curtain can be determined from the trajectory equations by letting $x = 0$ and $y = h$. The resulting equations for the air curtain range are:

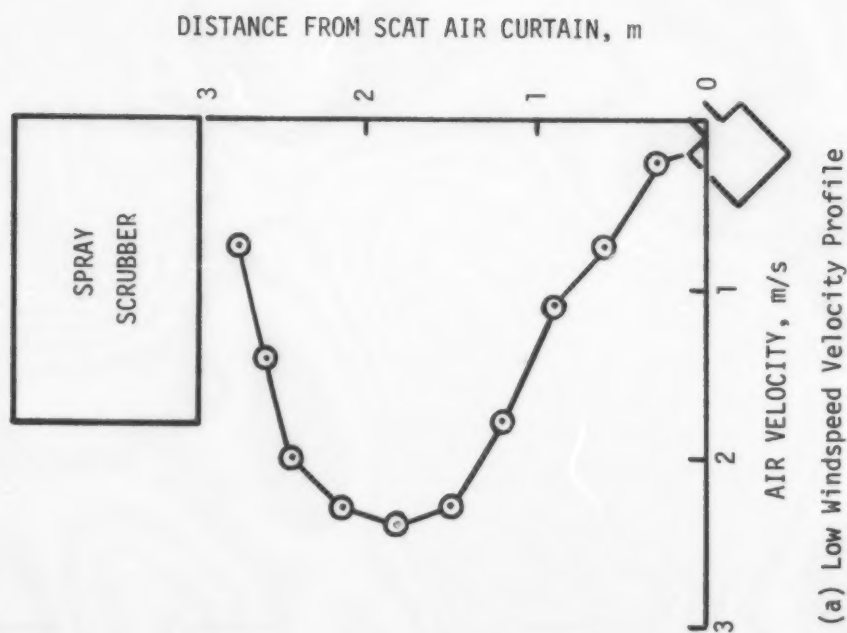
TABLE 9. AIR CURTAIN RANGE EXPERIMENTAL DATA
(Spray Scrubber to Air Curtain Separation = 3.1 m)

Run No.	SCAT air curtain nozzle slot width, cm	Centerline wind velocity, m/s	Average wind velocity, m/s	Air Curtain nozzle exit velocity, m/s	Incident Angle for complete wind deflection, degree	Angle at which maximum partial deflection occurred, degree
1	1.3	2.5	1.5	32	128	
2	1.3	2.5	1.5	38	114	
3	1.3	3.3	2.0	44	116	
4	1.3	2.5	1.5	49	110	
5	1.3	5.2	3.1	38	*	
6	1.3	4.7	2.8	44	*	
7	1.3	4.7	2.8	55	*	
8	1.3	4.7	2.8	63	*	
9	2.5	2.5	1.6	27	*	
10	2.5	2.5	1.6	38	129	
11	2.5	2.5	1.6	49	108	
12	2.5	4.9	3.1	27	*	
13	2.5	4.9	3.1	33	*	
14	2.5	4.9	3.1	38	*	
15	2.5	4.9	3.1	49	*	139
16	2.5	4.9	3.1	55	*	136
17	3.8	2.5	1.6	27	*	126
18	3.8	2.5	1.6	38	122	
19	3.8	2.5	1.6	48	116	
20	3.8	4.9	3.1	27	113	
21	3.8	4.9	3.1	38	*	
22	3.8	4.9	3.1	48	*	141
23	5.0	2.5	1.6	27	*	137
24	5.0	2.5	1.6	38	123	
25	5.0	2.5	1.6	46	110	
26	5.0	4.9	3.1	27	104	
27	5.0	4.9	3.1	38	*	129
28	5.0	4.9	3.1	46	115	
					111	

Note: *Wind could not be completely deflected



(b) High Windspeed Velocity Profile



(a) Low Windspeed Velocity Profile

Figure 65. Crosswind velocity profile at SCAT with no air curtain flow.

Abromovich:

$$\frac{h}{w} = 3.3 C_2 \left(\frac{u_{Gj}}{u_a} \right)^2 \cos^2 \alpha_0 \sin \alpha_0 \quad (110)$$

Volinsky:

$$h = - \frac{4w \cos \alpha_0}{C_4} \left(\frac{u_{Gj}}{u_a} \right)^2 \quad (111)$$

Ivanov:

$$\frac{h}{w} = 2 \left[- \cot \alpha_0 \left(\frac{u_{Gj}}{u_a} \right)^{2.6} \right]^{0.5} \quad (112)$$

Baturin (1972):

$$\frac{h}{w} = x^2 \left(\frac{u_{Gc}}{u_a} \right)^2 \quad (113)$$

Where h = range of air curtain, cm
 w = air curtain nozzle slot width, cm
 u_a = crosswind velocity, cm/s
 u_{Gj} = air curtain nozzle velocity, cm/s
 α_0 = initial incident angle, degree
 C_2 = constant, dimensionless
 C_4 = constant, dimensionless
 x = range coefficient, dimensionless

The range coefficient, x , in equation (113) is a function of incident angle and jet turbulent coefficient. Figure 66, given by Baturin (1972) and Maximov (1961), is a plot of the range coefficient versus incident angle with jet turbulent coefficient as parameter.

According to the equation of Volinski and of Ivanov, the maximum range is achieved when $\alpha_0 = 180^\circ$, i.e. the air curtain jet is directly opposing the wind. This is an unnatural arrangement. Therefore, Ivanov's equation cannot be applied to find the maximum range of the air curtain. In order for Volinsky equation to be applicable, the coefficient, C_4 , in his equation cannot be a constant for all possible incidence angles.

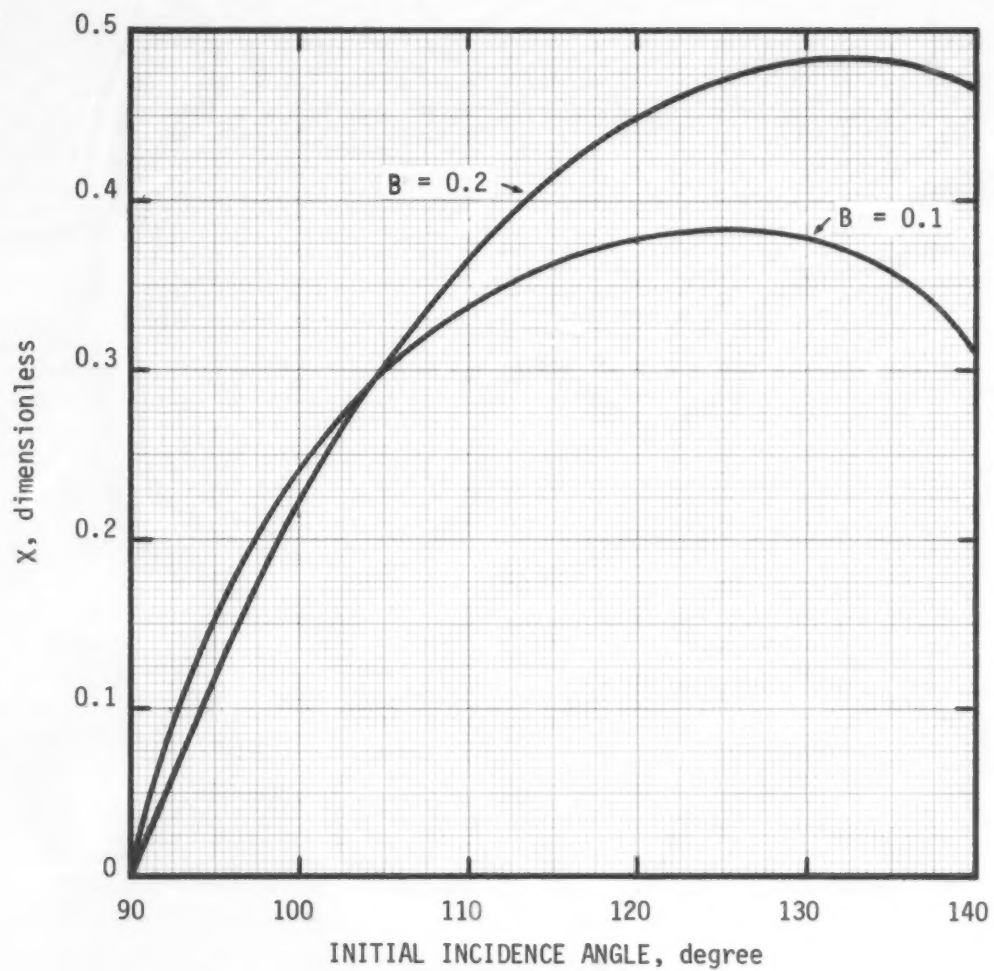


Figure 66. Range coefficient as function of incidence angle and jet turbulent coefficient.

A graphical presentation of Abramovich equation is shown in Figure 67. The maximum range of an air curtain is obtained when the incident angle is equal to 145° instead of 135° as can be derived from the laws of physics.

Table 10 shows the measured and predicted incident angles. The predicted wind deflection incident angle were calculated from Abramovich's equation with " C_2 " assumed to be one and Baturin's equation with the jet turbulence coefficient assumed to be 0.2. Predictions were based on both the average crosswind velocity and the centerline velocity.

As can be seen, predictions from Abramovich's equation correlate well with data. The agreement is better for predictions based on centerline air velocity (Figure 68).

In these experiments, crosswind was generated with an air curtain. The wind velocity distribution was measured at the plane formed by the spray scrubber and the SCAT air curtain. Since the SCAT air curtain was operated at an angle to the wind, the actual wind speed will be higher than that measured. This is probably the reason for better agreement between measured incident angle and the predicted angle based on the centerline velocity.

With a jet turbulent coefficient of 0.2, predictions from Baturin's equation do not agree with data. A larger value for the jet turbulent coefficient would give a better fit between data and predictions.

Based on the above evaluations, it seems that the equation of Abramovich is good enough for predicting the air curtain range. In SCAT applications, the air curtain should be arranged in such a way to obtain the maximum range. According to Abramovich (1961), the incident angle is 145° for maximum range. With this incident angle, Abramovich equation becomes:

$$\frac{h_m}{w} = 1.45 \left(\frac{u_{Gj}}{u_a} \right)^2 \quad (114)$$

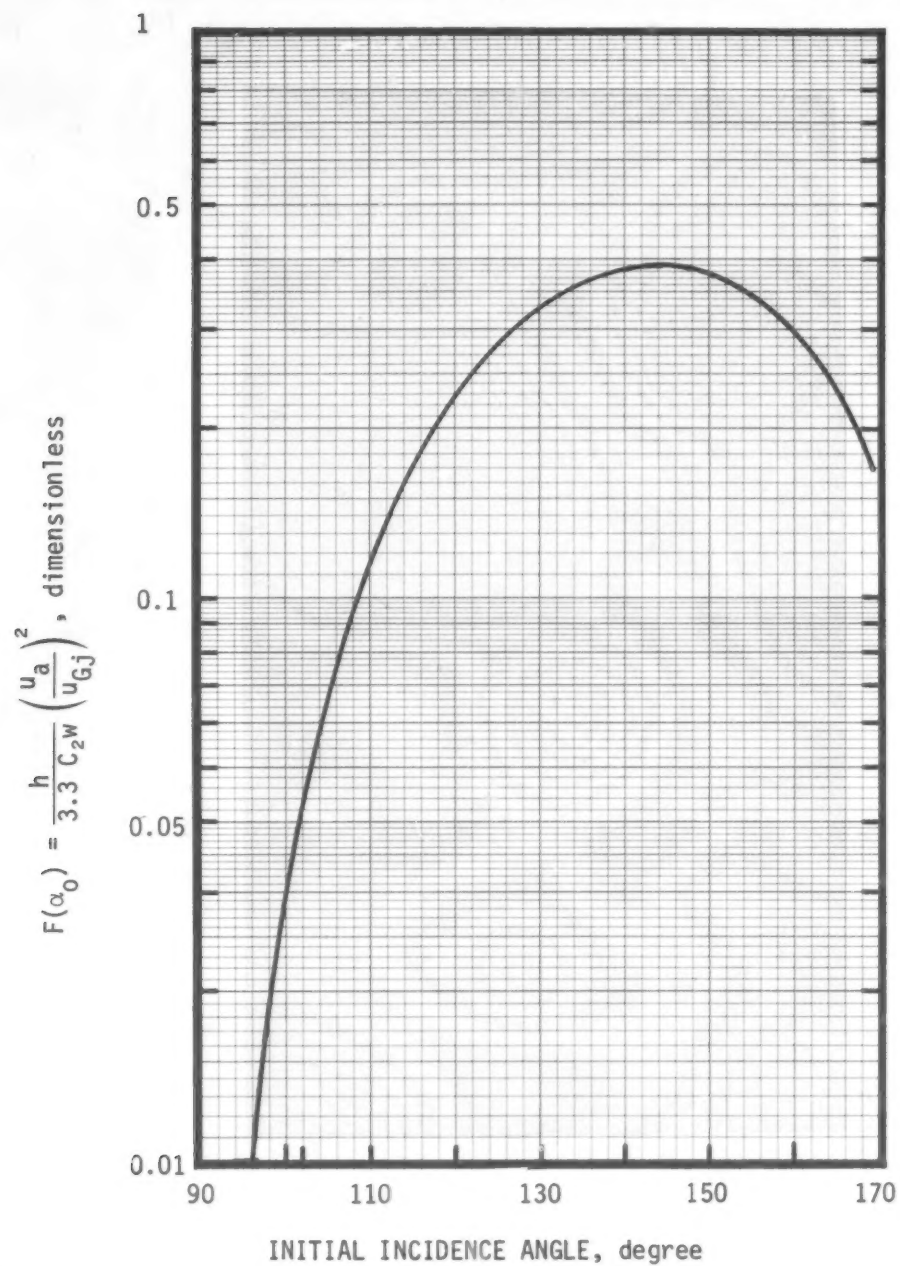


Figure 67. Predicted air curtain range versus initial incidence angle (Abramovich equation).

TABLE 10. MEASURED AND PREDICTED INCIDENT ANGLE FOR WIND DEFLECTION

Run No.	MEASURED INCIDENT ANGLE, degree Complete Deflection	Maximum Deflection	PREDICTED INCIDENT ANGLE, degree			
			Abramovich		Baturin	
			Centerline velocity Based on	Average velocity	Centerline velocity Based on	Average velocity
1	128		*	114.7	*	*
2	114		129.6	110.3	*	*
3	116		*	113.8	*	*
4	110		117.4	105.4	*	125
5	*		*	*	*	*
6	*		*	127.4	*	*
7	*		*	117.3	*	*
8	*		*	113.2	*	*
9	133		130.7	112.2	*	125
10	129		115.1	105.2	*	110
11	108		108.7	101.7	*	*
12	*		*	*	*	*
13	*		*	131.9	*	*
14	139		*	123.1	*	*
15	136		139.6	113.9	*	*
16	126		128.0	110.9	*	111.5
17	*		119.6	107.6	*	105
18	122		109.7	102.2	125	*
19	116		105.3	99.6	*	*
20	113		*	131.1	*	*
21	*		*	115.2	*	*
22	*	141	123.8	109.3	*	125
23	123	137	114.9	105.1	*	107
24	110		107.0	100.6	117	103
25	104		103.8	98.7	*	*
26	*	129	*	122.9	*	*
27	115		129.5	111.5	*	*
28	111		119.7	107.2	*	*

*Wind could not be deflected.

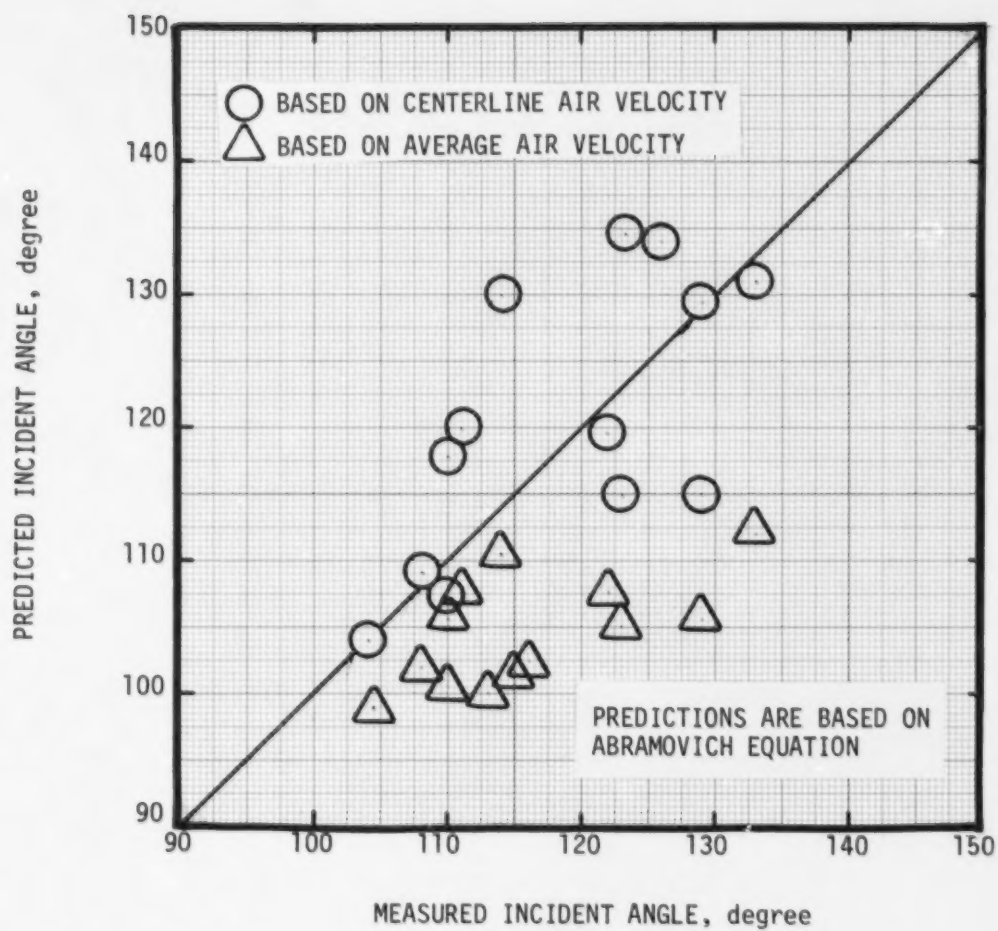


Figure 68. Measured and predicted incident angle for wind direction.

where h_m = maximum range of air curtain, cm
 w = air curtain slot width, cm
 u_a = crosswind velocity, cm/s
 u_{Gj} = air curtain slot velocity, cm/s

Outdoor Experiments - In indoor crosswind experiments, an air curtain was used to produce the crosswind. The use of an air curtain to simulate the crosswind has some shortcomings. It has a non-uniform velocity distribution and the wind direction does not change. In actual conditions, both the wind speed and direction fluctuate. For this reason, additional crosswind experiments were done outdoors.

Figure 69 shows the equipment arrangement. The spray and air curtain were so arranged that the wind direction was perpendicular to the spray scrubber and the air curtain jet discharged at 45° ($\alpha_0 = 135^\circ$) against the wind. Smoke or fly ash particles were injected at various locations for flow pattern observations.

Experiments were done for three air curtain and spray scrubber separations (3.1 m, 4.6 m, and 6.2 m), one air curtain slot width (5.1 cm), and four air curtain slot velocities (15.3 m/s, 20.3 m/s, 25.4 m/s, and 30.5 m/s). Experimental conditions and measured air curtain range are tabulated in Table 11. The range was estimated from the flow pattern of injected fly ash particles. If the smoke rose vertically, the wind was deflected from that location. Otherwise, it was not deflected. The air curtain range is the distance from the air curtain manifold to a nearest location where wind is not deflected.

Figure 70 shows the predicted and measured air curtain range. Data points for $h > L$ were not plotted. Predictions were based on the equation of Abramovich using an average wind speed. As can be seen, when the air curtain/spray scrubber separation, L , was larger than the air curtain range, h , the agreement between measurements and predictions is fair.

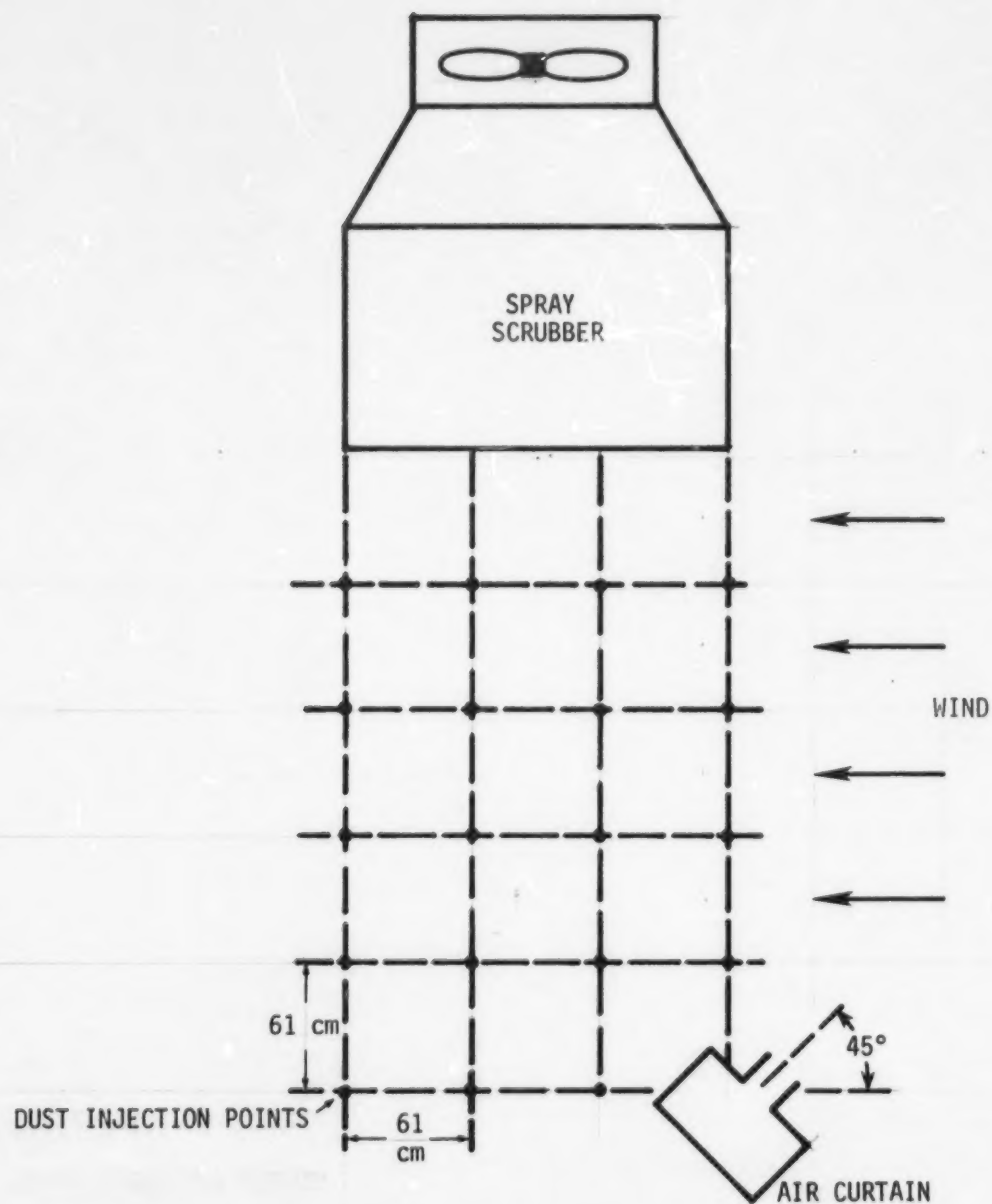


Figure 69. Outdoor crosswind experiment equipment layout.

TABLE 11. OUTDOOR CROSSWIND EXPERIMENTS

Air Curtain Slot Width: 5.1 cm

Initial Incident Angle: 135°

Wind Direction: Normal to spray scrubber

Run No.	Wind Speed m/s	Air Curtain Slot Velocity m/s	Air Curtain to Scrubber Separation m	Measured Air Curtain Range m
1	2.0 - 4.0	15.2	4.6	2.4
2	2.0 - 5.1	20.3	4.6	2.4
3	2.0 - 5.1	25.4	4.6	4.6
4	2.0 - 5.1	30.5	4.6	4.6
5	1.0 - 2.5	20.3	3.1	*
6	2.0 - 2.5	25.4	3.1	*
7	2.0 - 3.6	20.3	6.1	3.7
8	2.5 - 3.6	15.2	6.1	*
9	2.3 - 3.8	25.4	6.1	5.5
10	2.0 - 3.6	30.5	6.1	6.1
11	2.0 - 2.5	30.5	3.1	*
12	2.5 - 5.1	15.2	3.1	*

*Air curtain range is larger than air curtain/spray scrubber separation.

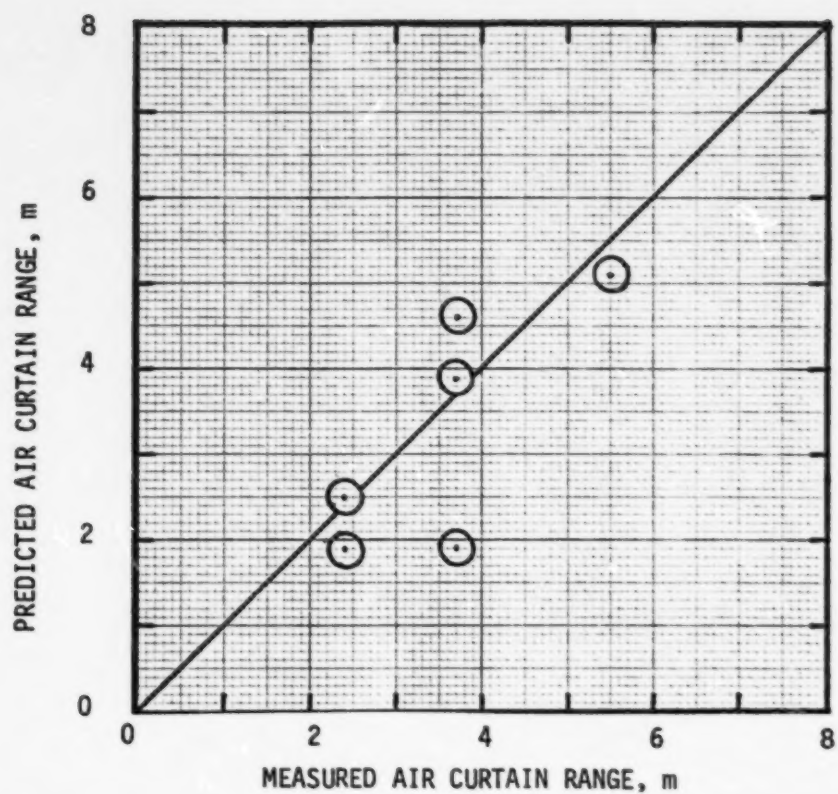


Figure 70 . Predicted and measured air curtain range (outdoor data).

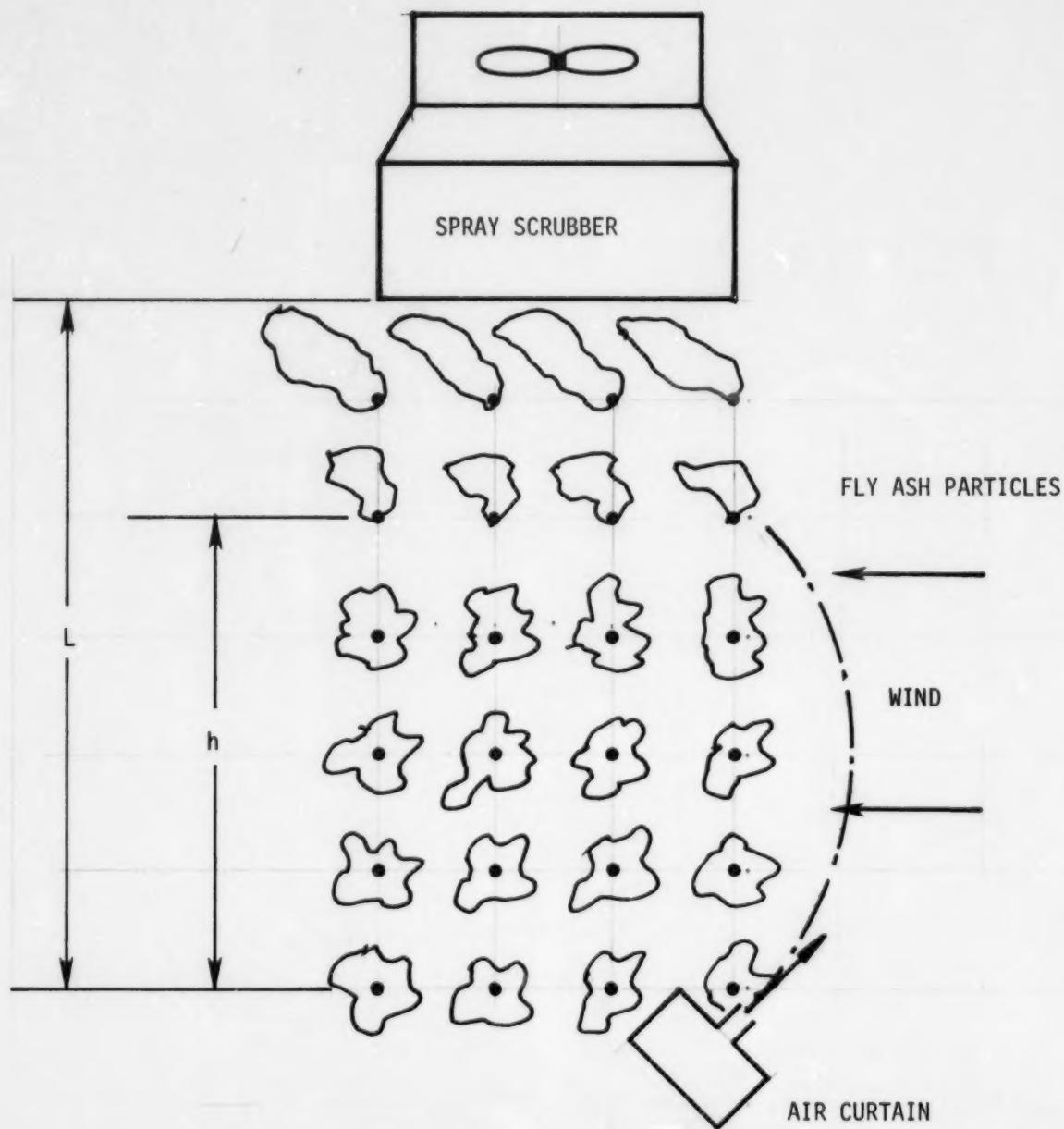
Figures 71 and 72 show some typical flow pattern of injected fly ash particles. When $L > h$ the dust was blown aside by the crosswind at location near the spray scrubber entrance. When $h > L$ and the slot exit velocities were above 25 m/s, some of the dust was entrained by the air curtain and missed the spray scrubber on the upwind side of the scrubber. Therefore, if an air curtain is solely used as wind screen, the air curtain jet stream should be at an angle 45° against the wind. The air curtain/spray scrubber separation should be smaller than the air curtain range. The air curtain should be located upwind of the source to avoid entraining the dust.

Air Curtain Trajectory

An air curtain could be used both for deflecting the crosswind and conveying the dust. Some possible arrangements are shown in Figure 73. The FPE source could be located in the pathway of the air curtain jet stream or located upwind of the air curtain. In the latter configuration, the wind will carry the dust into the jet stream.

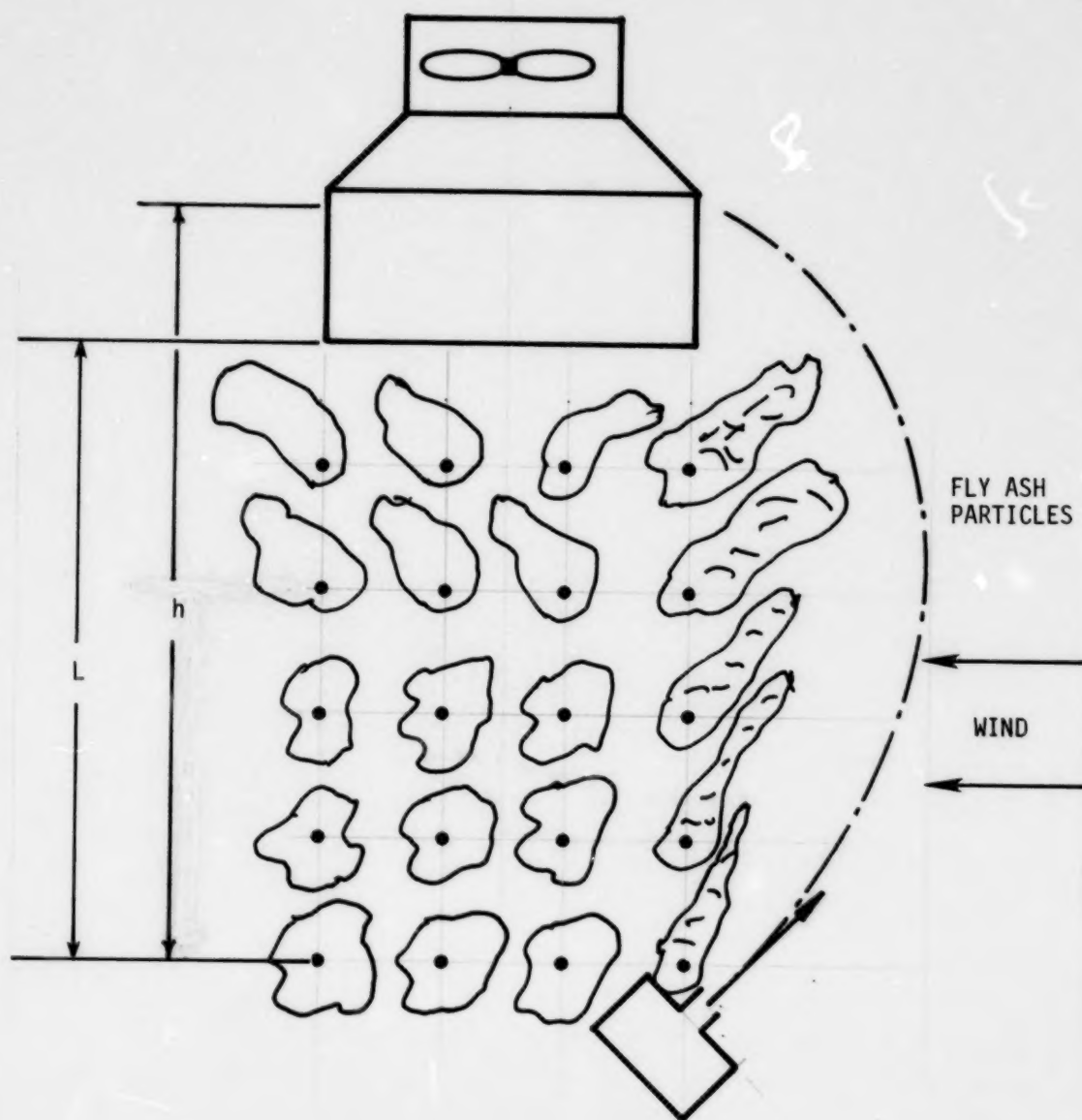
When an air curtain is used for deflecting the crosswind and conveying the dust, the trajectory of the air curtain jet stream is a more important parameter than the range of the air curtain. The trajectory of an air curtain was determined experimentally in two experiments.

The first experiment involved the determination of the air curtain centerline trajectory. Figure 74 shows the equipment layout. The spray scrubber was not used in these experiments and the air curtain slot was oriented vertically. In starting an experiment, the wind direction and speed were determined. Next, the air curtain was arranged so that its axis of discharge would make an angle " α_0 " with the wind direction. The air curtain slot width and discharge velocity were adjusted to desired values. Fly ash particles were injected into the fan for visual observation of air curtain jet stream shape. Velocity profiles were then measured at mid-level of the curtain for several downstream locations.



LOW AIR CURTAIN SLOT EXIT VELOCITY WITH $h < L$

Figure 71. Typical flow patterns of injected dust.



HIGH AIR CURTAIN SLOT EXIT VELOCITY WITH $h > L$

Figure 72. Typical flow patterns of injected dust.

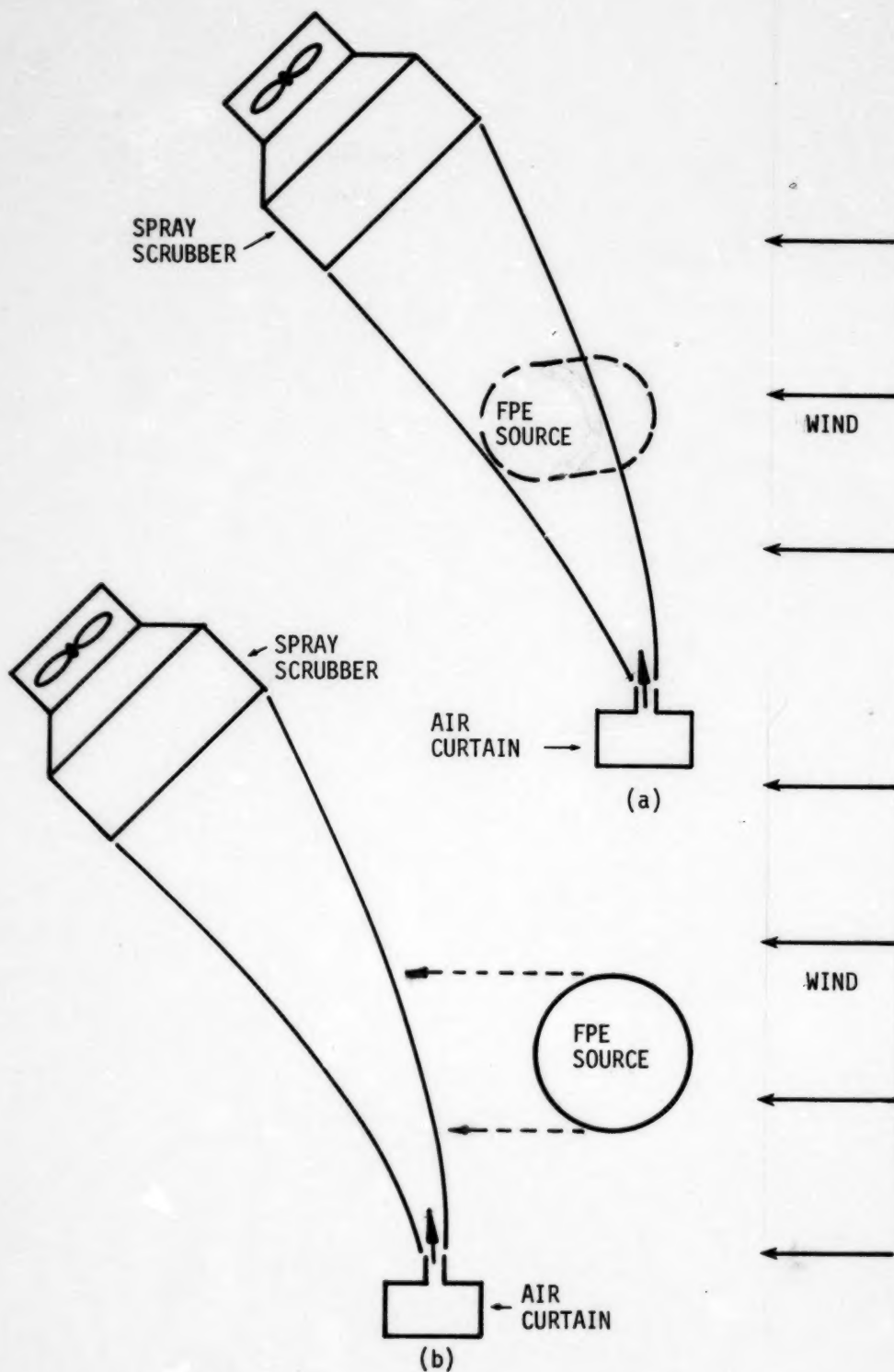


Figure 73. Possible arrangement of SCAT for containing FPE source.

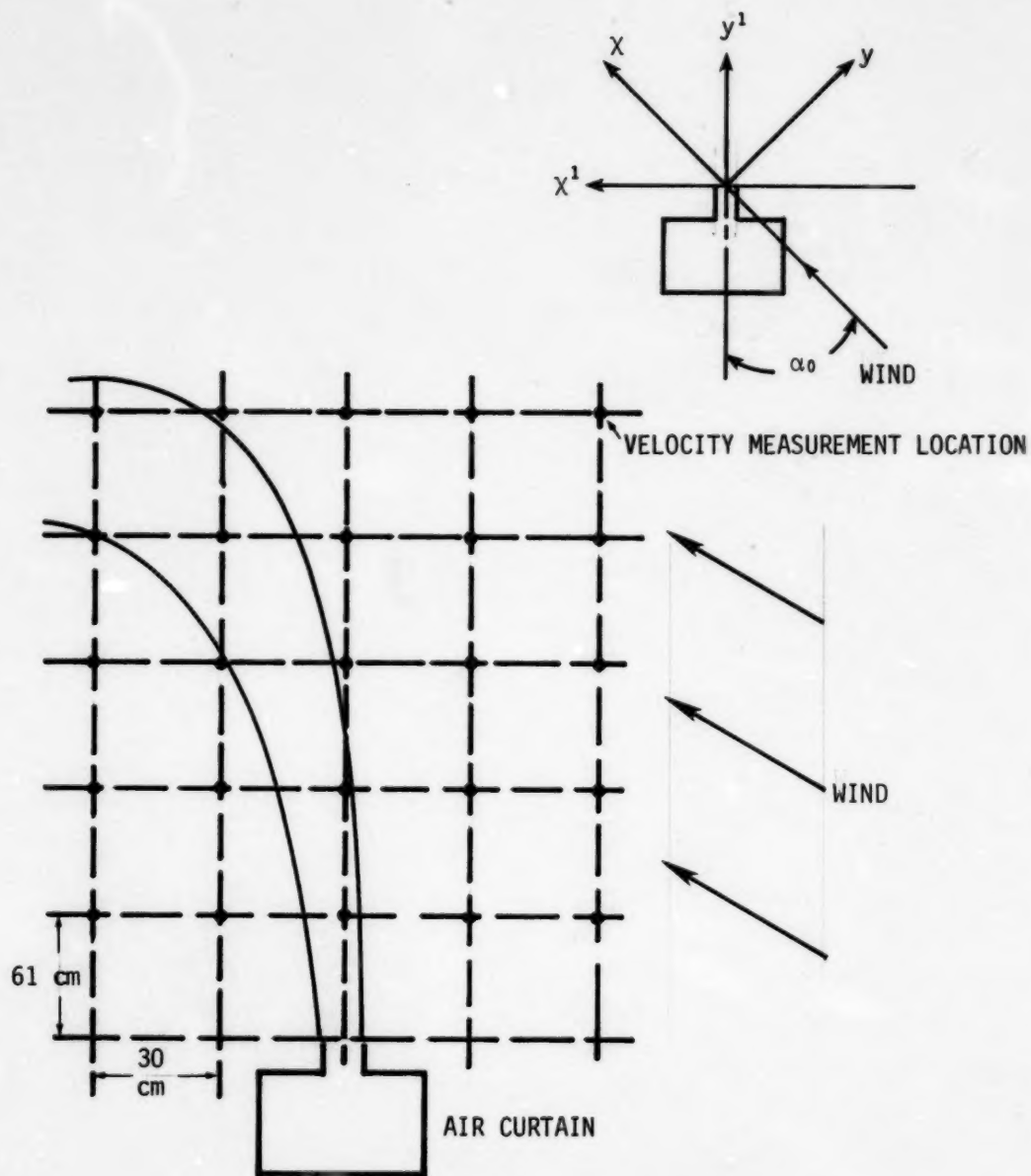


Figure 74. Air curtain trajectory measurement experimental setup showing coordinates relative to curtain jet axis.

The velocity profile was plotted to locate the maximum velocity, which corresponded to the velocity of air curtain axis. Figure 75 shows a typical measured velocity profile. It should be noted that the measured velocity profile was not the true velocity profile of the air curtain jet stream because measurements were not done on a plane perpendicular to the curved jet axis. Measurements were done on coordinates relative to jet exit axis (x' and y' in Figure 74).

Experiments were performed for three incident angle (30° , and 60° , and 90°) and four air curtain slot exit velocities (15.2, 20.3, 25.4, and 30.5 m/s). The air curtain slot width was 5.1 cm for all experiments. Table 12 shows the experimental data.

Since the air curtain jet stream is curved by the crosswind, one needs to rotate the spray scrubber to allow the jet stream and the dust to flow into the scrubber. The second experiment involved the measurement of the angle of rotation for various crosswind and wind/air curtain incident angles. The equipment layout is shown in Figure 76.

In starting an experimental run, the wind speed and direction of wind were measured. The air curtain, which was oriented vertically, was adjusted to obtain the desired slot exit velocity and incident angle. Fly ash particles were injected into the air curtain jet stream for visual observation of flow pattern. The spray scrubber was then rolled into position to intercept the jet stream. The location of spray scrubber relative to the air curtain and the angle of rotation relative to the wind direction for complete jet stream interception were measured and recorded.

Experiments were done for three wind/air curtain incident angles and four air curtain slot exit velocities. The air curtain slot width was fixed at 6.1 cm. Table 13 shows the data.

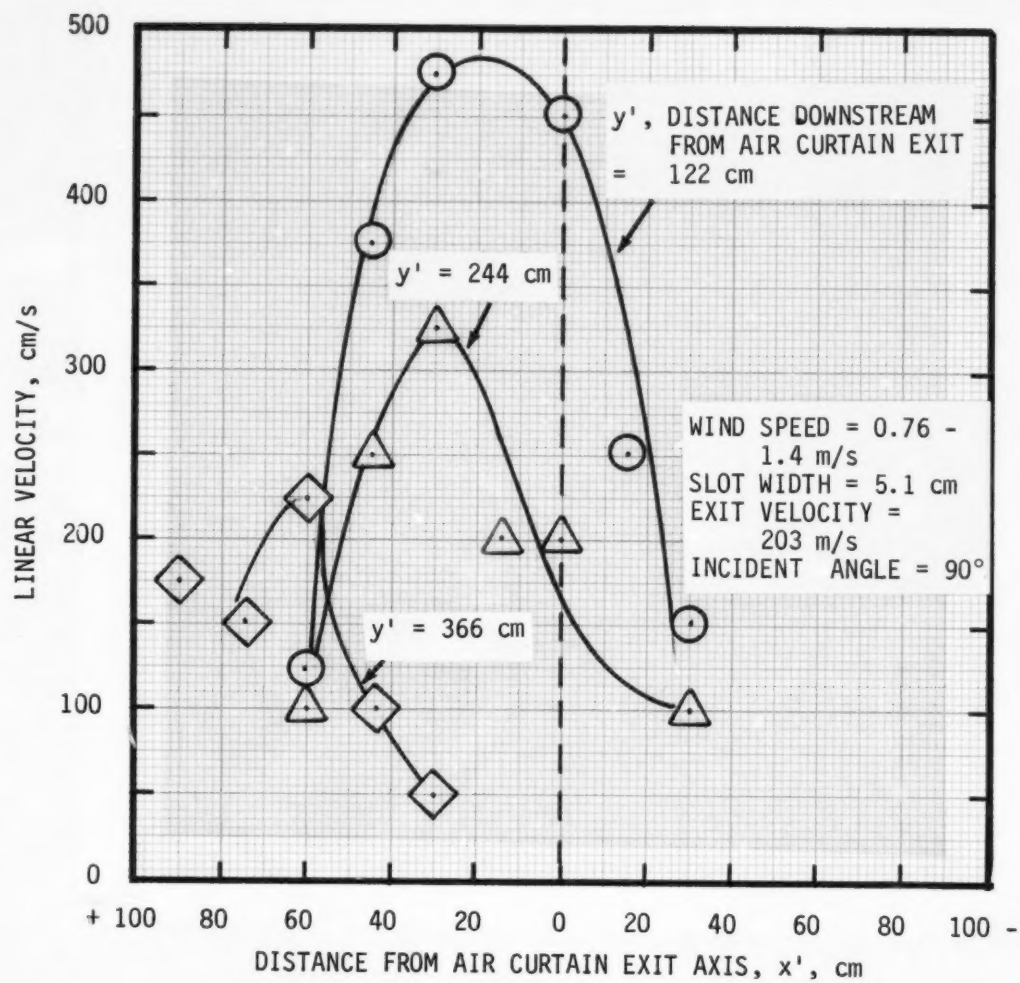


Figure 75. Measured velocity profile.

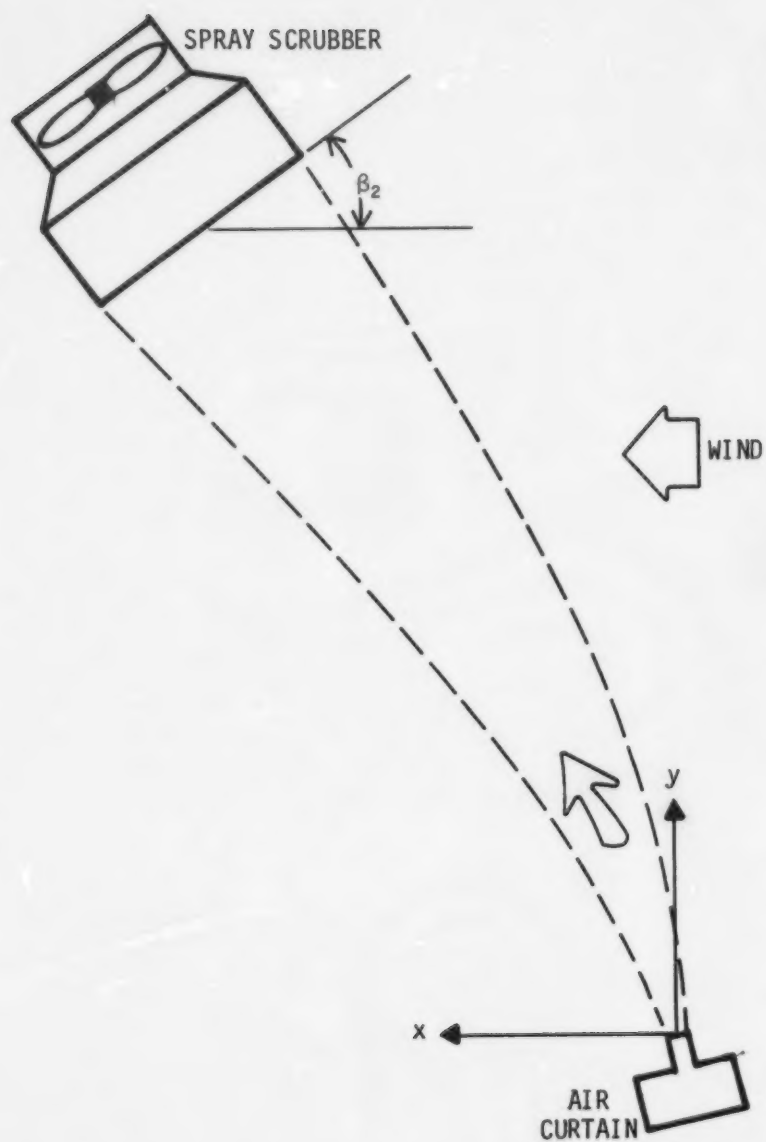


Figure 76. Air curtain trajectory measurement experimental setup.

TABLE 12. AIR CURTAIN TRAJECTORY DATA
Air Curtain Slot Width = 5.1 cm

Run No.	Wind Velocity, m/s	Air Curtain Slot Exit Velocity, m/s	Incidence Angle degree	Measured Location of Jet Axis, cm	
				x'	y'
70/23/1	0.7 - 1.4	20.3	90	3	61
				15	122
				21	183
				30	244
				61	366
	0.76 - 1.4	26.4	90	3	61
				15	183
				15	244
				23	305
				76.3	366
	0.5 - 1.5	15.2	90	15	183
				30	244
				30	305
				46	366
70/23/2	1.0 - 2.5	25.4	60	46	244
				46	366
				30	183
	1.0 - 2.5	30.5	60	15	244
				61	366
				52	488
				21	305
	1.0 - 2.5	20.3	60	15	122
				30	244
				30	366
70/23/3	0.5 - 2.3	15.3	30	15	122
				53	244
				40	427
	0.5 - 2.5	20.3	30	9	183
				61	427
				40	366
				46	305
	0.5 - 2.5	25.4	30	15	183
				38	305
				30	366
				30	427
	2.5	30.5	30	15	305
				61	427

TABLE 13. LOCATION AND ROTATION ANGLE OF SPRAY
SCRUBBER TO INTERCEPT AIR CURTAIN

Run No.	Wind Velocity, m/s	Air Curtain Slot Velocity, m/s	Incident Angle, degree	Measured Scrubber Location*, cm		Rotation Angle*, degree
				x	y	
70/23/4	2.5 - 5.1	30.5	90	259	305	58
	2.5 - 5.1	15.2	90	163	92	66
	2.5 - 5.1	25.4	90	229	290	58
	2.5 - 5.6	20.3	90	221	198	71
70/23/5	2.0 - 2.5	15.2	60	267	198	65
	2.0 - 2.5	20.3	60	262	203	70
	2.0 - 3.6	25.4	60	254	183	53
	1.8 - 2.5	30.5	60	254	183	45
70/23/6	2.5 - 4.6	15.2	30	226	41	76
	2.5 - 4.6	20.3	30	351	153	62
	2.5 - 3.6	25.4	30	351	153	62
	2.5 - 3.6	30.5	30	341	183	75

*See Figure 76 for definition

Comparison Between Theory and Data

Equation of Abramovich (equation 106) can be written in the following form

$$F(\alpha_0) = \left(\frac{2x}{w} - \frac{2y}{w} \cot \alpha_0 \right) \frac{u_a}{u_{Gj}} (\sin \alpha_0)^{1.5} = \frac{0.39}{C_2^{0.5}} \left(\frac{2y}{w} \right)^{1.5} \quad (115)$$

where u_a = crosswind velocity, cm/s
 u_{Gj} = air curtain slot exit velocity, cm/s
 w = air curtain slot width, cm
 x = coordinate in the direction of wind, cm
 y = coordinate perpendicular to wind direction, cm
 α_0 = initial incident angle, degree

Figures 77 and 78 show the data and predictions by equation (115). The constant " C_2 " was assumed = 1. It should be noted that the coordinate systems used in equation (115) and in the experiments were different. In equation 115, the x -coordinate is in the direction of wind and the y -coordinate is in the direction perpendicular to the wind. In the experiments, x' - and y' -coordinates were taken to be the direction normal to the air curtain exit axis and in the direction of jet exit, respectively. In other words, " x' " and " y' " are relative to the curtain jet exit axis and " x " and " y " are relative to the wind direction. For identification, they were labeled " x' " and " y' " axes in Figure 74. Since the measured location of the jet axis was in x' - and y' -coordinates, they were transformed to x - and y -coordinates by the following formulas:

$$x = [(x')^2 + (y')^2]^{0.5} \cos (\beta - 90 + \alpha_0) \quad (116)$$

$$y = [(x')^2 + (y')^2]^{0.5} \sin (\beta - 90 + \alpha_0) \quad (117)$$

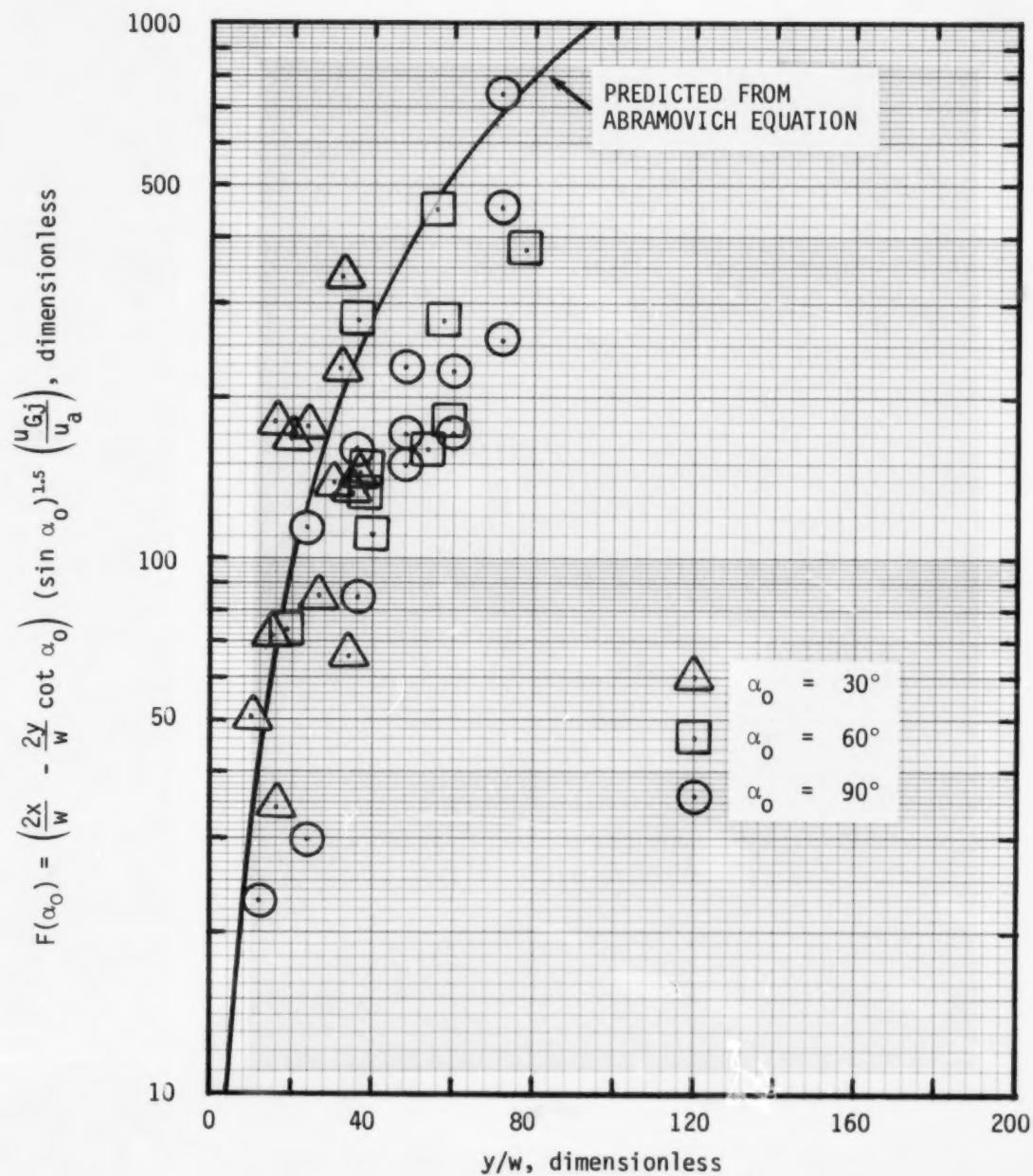


Figure 77. Measured and predicted air curtain trajectories relative to the wind direction.

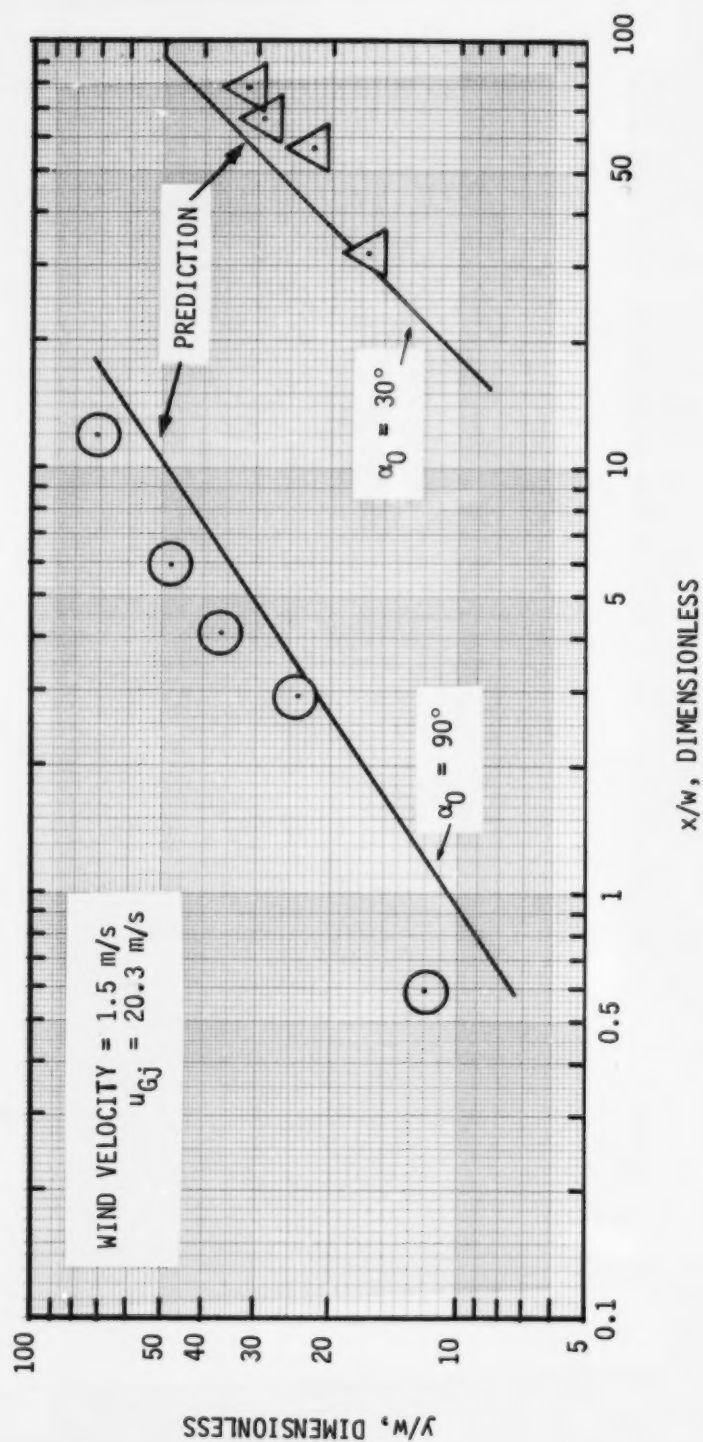


Figure 78. Measured and predicted air curtain trajectory.

where $\beta = \tan^{-1} \frac{y'}{x'}$

α_0 = incident angle, degree

x = coordinate in the direction of wind, cm

y = coordinate perpendicular to wind, cm

x' = coordinate perpendicular to air curtain jet exit, cm

y' = coordinate in the direction of air curtain jet exit, cm

It can be seen from Figures 77 and 78 that the agreement between measured and predicted trajectory of air curtain axis is fair.

In order for the spray scrubber to intercept the air curtain jet stream, its entrance face should be normal to the local tangent line of the jet axis. By differentiating equation 106, the following equation is obtained:

$$\frac{2dx}{w} = \frac{2dy}{w} \cot \alpha_0 + \frac{0.39}{C_2^{0.5} (\sin \alpha_0)^{1.5}} \left(\frac{u_a}{u_{Gj}} \right)^{(1.5)} \left(\frac{2y}{w} \right)^{0.5} \left(\frac{2dy}{w} \right) \quad (118)$$

The local tangent is then given by the following expression:

$$\frac{dy}{dx} = \frac{1}{\cot \alpha_0 + \frac{0.585}{C_2^{0.5} (\sin \alpha_0)^{1.5}} \left(\frac{u_a}{u_{Gj}} \right) \left(\frac{2y}{w} \right)^{0.5}} \quad (119)$$

The angle of rotation of the spray scrubber relative to the wind direction is equal to

$$\beta = 90 - \tan^{-1} \frac{dy}{dx} \quad (120)$$

where β = spray scrubber rotation angle relative to wind direction, degree

$\frac{dy}{dx}$ = slope of air curtain jet stream axis, cm/cm

Figure 79 shows a plot of measured angle of rotation versus calculated angle. The calculations were based on average wind speed. As can be seen, the measured angle is slightly larger than that calculated. However, in view of the method (visual judgment) used in the experiments for determining whether the jet stream was intercepted by the spray scrubber, the discrepancy between measurement and prediction is considered to be acceptable.

Crosswind Experiment Conclusions

It has been shown that the equation of Abramovich gave reasonable predictions on the range of air curtains, trajectory of air curtain axis in the presence of crosswind, and the required spray scrubber rotation angle for intercepting the jet stream. Therefore, in operating the SCAT system under crosswind conditions the Abramovich equation can be used for locating the air curtain, and the spray scrubber relative to the crosswind and FPE source.

HOT SOURCE EXPERIMENTS

In some metallurgical processes, such as the iron and steel manufacturing process, very hot plumes containing very high concentration of particles are released. The most efficient and economical way of cleaning these plumes would be to capture them at the source where the concentrations are highest and gas volume smallest. However, for practical reasons, e.g. presence of overhead cranes, it is impossible to capture the plume at the source or even vertically above the source with fume hoods. In these situations a SCAT system could be applied to control the emissions. An air curtain could be used as the ceiling to contain the fumes and dust and horizontally displace the plume into a receiving hood or directly into a scrubber if space is available for its installation.

In these applications, the hot plume can change the air curtain flow field. The effects of the buoyancy-induced flow on air curtain design parameters and performance were studied experimentally in the present study.

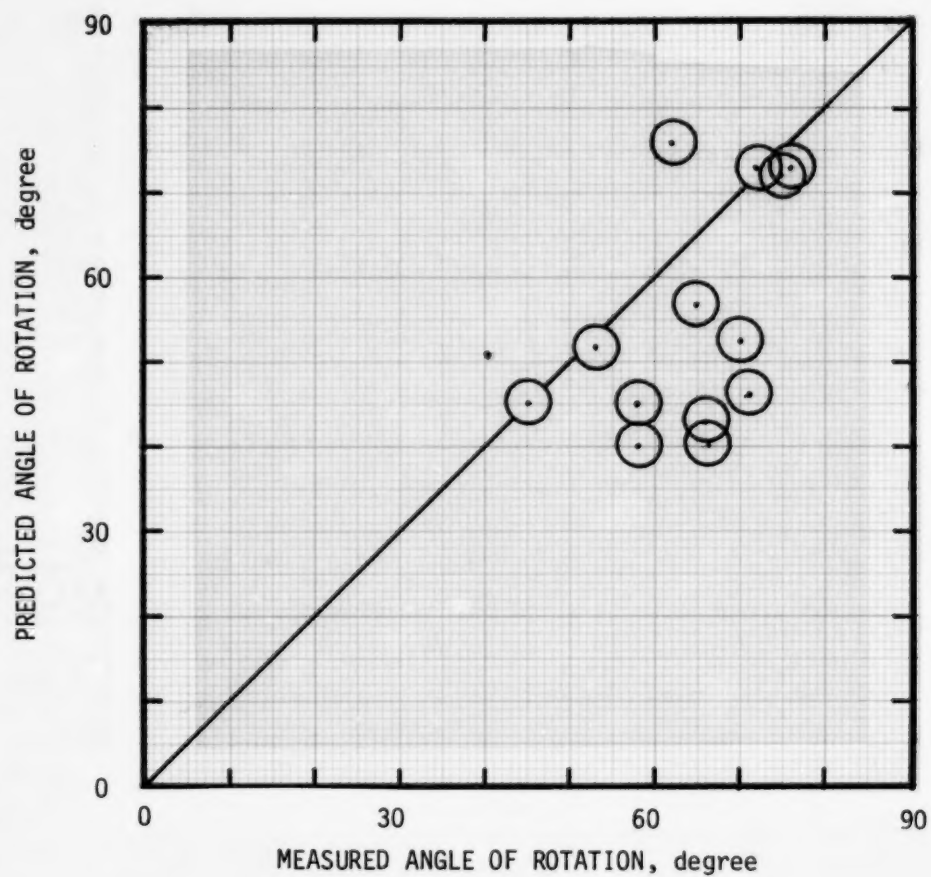


Figure 79. Measured and predicted angle of rotation of spray scrubber to intercept air curtain jet stream.

Literature Search

The dynamics of containing a hot buoyant plume with an air curtain is similar to a chimney plume discharged into the atmosphere in the presence of a crosswind. Therefore, the theories developed for plume rise could be applied to the SCAT for controlling the emissions from a hot source.

The literature are crowded with plume rise formulae. The following is a brief review of the available theoretical and empirical correlations.

Vertical Rise of Force Plume in Calm Environment

When an air curtain is placed on top of the source for diverting the plume, the plume first rises vertically until it meets the air curtain jet stream, then it becomes a bent-over plume. If the hot source is located outdoors, such as a coke oven, another air curtain might be needed for deflecting the interfering crosswind.

The plume generated from a source of finite size which delivers a flux of mass, momentum, and buoyancy has been termed a forced plume by Morton (1959). The vertical rise of the forced plume has been studied theoretically by Morton (1959) and Crapper and Baines (1977, 1978). As the plume rises, the surrounding air is entrained towards the axis of the plume and mixed into the plume, i.e. the plume grows in size as it rises. The plume rising velocity and diameter can be theoretically predicted from the three equations expressing the conservation of mass, momentum, and energy if the rate of entrainment into the plume is known. Many researchers, e.g. Morton (1959), Turner (1973), assumed that the entrainment velocity " u_e " is related to the local centerline velocity " u_{Gc} " by a single experimental entrainment coefficient " α ", i.e.

$$u_e = \alpha u_{Gc} \quad (121)$$

where, u_e = local inward entrainment velocity, cm/s
 u_{Gc} = local plume centerline velocity, cm/s
 α = entrainment coefficient, dimensionless

This assumption is based on the observation that turbulent jets and plumes grow linearly with height above the source. Linear growth of radius with height implies that the mean inflow velocity across the edge of the turbulent plume is proportional to local upward velocity.

Morton (1965) stated that the rate of entrainment into a strongly buoyant plume is proportional to the local density ratio " ρ_{Gc}/ρ_a " and should also depend on the lateral diffusion of momentum and hence the Reynolds stresses, which have local magnitude proportional to " $\rho_{Gc} u_{Gc}^2$ ". In mathematical form, it is expressed as:

$$u_e = \alpha \left(\frac{\rho_{Gc}}{\rho_a} \right)^{0.5} u_{Gc} \quad (122)$$

where u_e = local inward entrainment velocity, cm/s
 u_{Gc} = local centerline velocity, cm/s
 ρ_{Gc} = local centerline density, g/cm³
 ρ_a = ambient air density, g/cm³
 α = entrainment constant, dimensionless.

Additional support for this assumption comes from Ricon and Spalding (1961), whose experimental results suggested that the rate of entrainment may be locally proportional to " $\rho_{Gc}^{0.5}$ ", and Townsend (1966), whose theoretical results suggested that the entrainment rate is proportional to " $(\rho_{Gc}/\rho_a)^{0.5}$ ". A similar assumption has been made by Thomas (1963) and Steward (1970).

The numerical value for " α " can not be obtained theoretically and it must be determined experimentally. Morton (1959) adopted a value of 0.116 for "top hat" velocity profiles, based on measurements in non-buoyant jets. Rincon and Spalding (1961), by directly measuring the inflow, found a value of " α " for jets closer to 0.008. For plumes with Gaussian velocity and density distributions, Turner (1973) deduced $\alpha = 0.085$. By comparing theory and data for plumes with Gaussian distribution, Morton (1959) recommended a value of 0.082 for " α ". Liste and Imberger (1973) have shown that " α " for a forced plume is

not a constant; but it is linearly dependent on the inverse of the local jet Froude number. Since "a" does not enter sensitively into many calculations, $\alpha = 0.08$ might be adopted for all cases.

With the entrainment velocity given by either equation (121) or equation (122), the three conservation equations can be expressed in cylindrical polar coordinate (z,r) as follows:

1. Mass

$$\frac{d}{dz} \int_0^{\infty} 2\pi r u_G \rho_G dr = 2\pi b \rho_a u_e \quad (123)$$

2. Vertical momentum

$$\frac{d}{dz} \int_0^{\infty} 2\pi r u_G^2 \rho_G dr = \int_0^{\infty} 2\pi r g (\rho_a - \rho_G) dr \quad (124)$$

3. Buoyance flux

$$\frac{d}{dz} \int_0^{\infty} 2\pi r u_G (\rho_a - \rho_G) dr = 0 \quad (125)$$

where b = plume radius, cm
 r = coordinate from plume axis, cm
 z = coordinate from top of source, cm
 u_G = local plume upward velocity, cm/s
 u_e = local inward entrainment velocity, cm/s
 g = gravitational constant, cm^2/s^2
 ρ_a = ambient air density, g/cm^3
 ρ_G = local gas density, g/cm^3

Equations (123) through (125) can be solved if the velocity and gas density profiles of the plume are known. Rouse, et al. (1952) determined the profiles experimentally and found that the mean profiles were closely Gaussian but with different lateral spread. Thus, the vertical velocity and gas density profiles can be written as:

$$u_G = u_{Gc} \exp \left(-\frac{y^2}{b^2} \right) \quad (126)$$

$$\rho_a - \rho_G = (\rho_a - \rho_{Gc}) \exp \left(-\frac{\lambda^2 y^2}{b^2} \right) \quad (127)$$

where b = plume radius, cm
 u_G = local plume upward velocity, cm/s
 u_{Gc} = plume centerline velocity, cm/s
 ρ_a = ambient air density, g/cm³
 ρ_G = plume gas density, g/cm³
 ρ_{Gc} = plume centerline gas density, g/cm³
 λ = buoyancy spreading factor, dimensionless

The buoyancy spreading factor, λ , is determined from experiments, Rouse, et al. (1952) found that for weakly buoyant plumes, $\lambda = 1.16$.

Equation (125) states that in a uniform density environment, the buoyancy flux across an infinite horizontal plane is invariant with height, i.e., buoyancy flux is conserved. Substituting equations (126) and (127) into equation (125) and performing the required integration and differentiation, the following equation is obtained

$$F = F_0 \frac{\pi \lambda^2 b_0^2 u_{Gc0}^2 g (\rho_a - \rho_{Gc0})}{(1 + \lambda^2) \rho_a} \quad (128)$$

where F = buoyancy flux, cm⁴/s³
 F_0 = buoyancy flux at plume exit, cm⁴/s³
 b_0 = plume radius at exit, cm
 u_{Gc0} = plume centerline velocity at exit, cm/s
 g = gravitational acceleration, cm/s²
 ρ_a = ambient air density, g/cm³
 ρ_{Gc0} = plume centerline gas density at exit, g/cm³
 λ = buoyancy spreading factor, dimensionless

The plume radius and velocity at any location above the source can be obtained by solving equations (123) and (124) simultaneously. Crapper and Baines (1977) solved these two equations numerically for both expressions of inward entrainment velocity (equations 121 and 122). They found that both expressions result in approximately the same plume radius and velocity if the initial fractional density difference $(\rho_a - \rho_{GC})/\rho_a$, does not exceed 0.05.

Morton (1959) used the simpler expression for inward entrainment velocity (equation 121) and obtained analytical solutions for equations (123) and (125). He showed that the turbulent flow generated by a finite source which emits fluxes of mass and momentum at a steady rate can be related to the flow from a virtual point source of buoyancy and momentum only. Therefore, by specifying two physical parameters at the source, e.g., the rates of discharge of buoyancy and momentum, all other variables of the plume can be scaled in terms of these. Morton's solutions for a forced plume from a virtual point source of buoyancy and momentum are:

$$Q_G^* = 1.265 (u_G^*)^{2.5} \quad (129)$$

$$z^* = 2.108 (u_G^*)^{1.5} - 1.057 \quad (130)$$

where:

$$u_G^* = \frac{b u_{GC}}{b_o u_{GCo}} \quad (131)$$

$$z^* = \frac{1.68 \alpha^{0.5} (1+\lambda^2)^{0.5} F_o^{0.5} z}{\left(\frac{b_o u_{GCo}}{1.41} \right)^{1.5}} = \frac{\alpha^{0.5} F_o^{0.5} z}{M_o^{0.75}} \quad (132)$$

$$Q_G^* = \frac{b^2 u_{GC} (1+\lambda^2)^{0.5} F_o^{0.5}}{1.68 \alpha^{0.5} (b_o u_{GCo})^{2.5}} \quad (133)$$

where M_o = momentum flux at exit, cm^4/s^2
 F_o = buoyancy flux at plume exit, cm^4/s^3
 b = plume radius, cm
 u_{Gc} = plume centerline velocity
 b_o = plume radius at exit, cm
 u_{Gco} = plume centerline velocity, cm/s
 z = vertical distance from exit, cm
 α = entrainment constant, dimensionless
 λ = buoyancy spreading factor, dimensionless
 u_G^* = dimensionless plume rising velocity, dimensionless
 Q_G^* = dimensionless plume volumetric flow rate, dimensionless
 g = dimensionless height above source, dimensionless

Figure 80 shows curves for the dimensionless plume radius and dimensionless centerline velocity. Dimensionless plume radius and centerline velocity are defined as

$$b^* = \frac{Q_G^*}{u_G^*} = \frac{F_o^{0.5} b}{\alpha^{1/2} M_o^{0.75}} \quad (134)$$

$$u_{Gc}^* = \frac{4(u_G^*)^2}{Q_G^*} = \frac{\alpha^{0.5} M_o^{0.25} u_{Gc}}{F_o^{0.5}} \quad (135)$$

where b^* = dimensionless plume radius, dimensionless
 u_{Gco}^* = dimensionless centerline velocity, dimensionless
 F_o = buoyancy flux at exit, cm^4/s^3
 M_o = momentum flux at exit, cm^4/s^2
 b = plume radius, cm
 u_{Gc} = plume centerline velocity, cm/s
 α = entrainment constant, dimensionless

As can be seen from Figure 80, the growth rate of the plume is at first like that in a pure jet, but as the momentum generated by buoyancy dominates

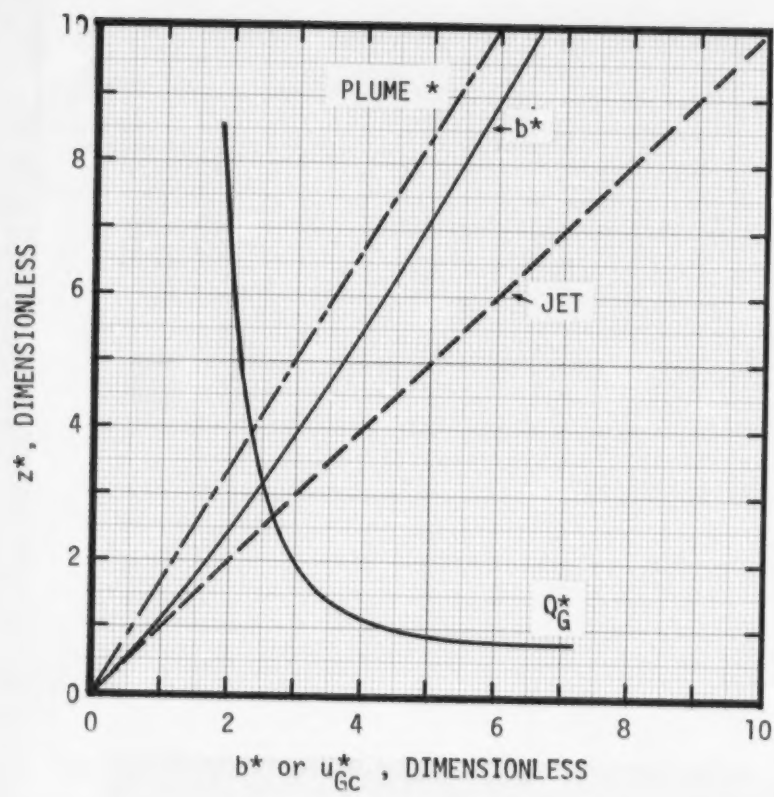


Figure 80. The behavior of a forced plume in a uniform environment.

over the initial momentum, the growth rate becomes smaller and approaches that of a pure plume.

Buoyant Forced-Plumes in Cross Flow

As the plume rises to meet the air curtain jet stream, the plume will bend over due to the addition of cross flow momentum. The plume takes on the horizontal air velocity quickly after meeting the jet stream, but continues to rise with its original vertical momentum plus momentum added by buoyancy. Therefore, in order for one to properly locate the receiving hood of the spray scrubber to intercept the plume, one should know about the height of the plume rise and plume dimensions.

There are many publications dealing with the prediction of plume rise. In the following, simple formulae for plume rise prediction will be summarized. Complicated models, such as that given by Schatzmann (1979), Hirst (1972), and Fan (1967) will not be presented.

For a buoyant plume with negligible exit momentum flux, Scorer (1958, 1978) approximated the plume as a point source of buoyancy flux being sheared over by the cross flow. Locally, each section of the plume rises like a line thermal with a strength equal to the buoyancy flux at the exit divided by the velocity of the cross flow. Following this argument, the vertical rise of the plume in a neutrally stable cross flow was deduced from dimensional analysis by Briggs (1965). Moore (1966) and Slawson and Csanady (1967) derived the same equation theoretically, based on the entrainment concept introduced by Morton et al., (1956).

$$z = \frac{C_5 F_o^{1/3} x^{2/3}}{u_a} \quad (136)$$

$$\text{or } \frac{z}{L_B} = \left(\frac{3}{2\alpha^2}\right)^{1/3} \left(\frac{x}{L_B}\right)^{2/3} \quad (137)$$

where z = vertical rise of plume, cm
 x = horizontal distance downstream of source, cm
 u_a = crosswind velocity, cm/s
 F_o = buoyancy flux at source, cm^4/s^3
 L_B = F_o/u_G^3 = buoyancy length, cm
 C_5 = empirical constant, dimensionless
 α = entrainment constant, dimensionless

Equation (137) is the well known two-thirds law which states that the plume rise is proportional to " $x^{2/3}$ ".

The buoyancy flux at the source " F_o " can be calculated from equation (128) or from the following formula:

$$F_o = \frac{g Q_H}{\pi c_p \rho_G T} = g \left(\frac{\rho_a - \rho_G}{\rho_a} \right) A u_G \quad (138)$$

where F_o = buoyancy flux at source cm^4/s^3
 Q_H = heat flux emitted at source, $\text{cal}/\text{cm}^2\text{-s}$
 c_p = specific heat, $\text{cal}/\text{g} - ^\circ\text{C}$
 ρ_G = gas density, g/cm^3
 T = gas temperature, $^\circ\text{K}$
 u_G = plume rising velocity, cm/s
 g = gravitational acceleration, cm/s^2
 A = cross-sectional area of the plume, cm^2

Equation (137) has been verified with data collected from stack plumes of large power plants. However, the empirical constant varies. Table 14 shows the value for the empirical constant by several investigators. Of all the values, the one suggested by Briggs (1969) i.e., $C_5 = 1.6$, is the most frequently used.

Equation (137) is applicable for $x \gg L_B$. For the initial stage of plume rise, Slawson and Csanady (1967) and Bringfelt (1969) recommended the same

TABLE 14. Value For The Empirical Constant in Equation (136)

<u>Investigators</u>	<u>C₅</u>
Briggs (1965)	2.0
Briggs (1969)	1.6
Thomas, et al., (1969)	1.04
Fay, et al., (1970)	1.32
Bringfelt (1969)	1.75

equation but with a different value for the empirical constant. The constant is 2.3, according to Slawson and Csanady (1967), and $(3.61/\alpha^2)^{1/3}$, according to Bringfelt (1969).

Other forms of correlation are also reported in the literature, e.g., Moore (1974). Those correlations will not be presented here.

Equation (137) is applicable to plumes with weak buoyancy. For forced plumes, it is not valid. Chu and Goldberg (1974) used a simple entrainment model, which is similar to the one used by Slawson and Csanady (1967) but including the effects of both exit momentum and buoyancy, to obtain the following equation for the plume trajectory.

$$z = \left(\frac{3}{4\alpha^2} \right)^{1/3} \left[\frac{1}{2} \left(\frac{4F_0}{\pi \rho_a u_a^3} \right) x^2 + \left(\frac{4M_0}{\pi \rho_a u_a^2} \right) x \right]^{1/3} \quad (139)$$

where z = height of plume rise, cm
 x = horizontal distance from source, cm
 u_a = cross flow velocity, cm/s
 F_0 = exit buoyancy flux, cm⁴/s³
 M_0 = exit momentum flux, cm³/s²
 ρ_a = ambient air density, g/cm³
 α = entrainment constant, dimensionless

By comparing their own laboratory data and data of Fan (1967), Chu and Goldberg (1974) recommended that $\alpha = 0.5$, which is the same as that used by Slawson and Csanady (1967).

Equation (139) reduces to equation (137) if exit momentum flux, M_0 , is negligible. If exit buoyancy is neglected, equation (139) would give the one-third power law for the plume rise as pointed out by Scorer (1958) for a pure momentum jet.

Mathematical Modeling

The plume trajectory equations presented earlier assumed a constant cross-wind velocity. In containing a hot plume with an air curtain, the cross flow is not a constant; but decreases in the downstream direction. Therefore, the reported trajectory equations might not be applicable to the situation of containing a hot buoyant plume with air curtains. In the following, a new trajectory formula is derived. The approach used in the derivation is similar to that of Chu and Goldberg (1974).

Consider a forced-plume rising vertically from a source. At some location above the source, the plume encounters the air curtain jet stream. At this location, which will be the origin of the coordinates (x,z) , the density of plume, vertical velocity, momentum flux, and buoyancy flux are " ρ_{G0} ", " v_{G0} ", " M_0 ", and " F_0 ", respectively. All these parameters can be calculated from equations presented in the section on vertical rise of forced plume. The density of the air curtain jet stream is assumed to be uniform and its velocity distribution is that given by equation (35). To simplify the analysis, the plume is replaced by a point source of momentum flux " M_0 " and buoyancy flux " F_0 ", located at the origin of the coordinates (x,z) . The local density and velocity along the plume are ρ_G and (u_G, v_G) , respectively. Figure 81 shows a sketch of the definitions.

Assumptions which are made in the derivation are:

1. The horizontal velocity components in the plume are approximately equal to the air curtain jet stream.
2. The pressure drag is negligible compared with the contribution from turbulent entrainment.
3. The velocity distribution of the air curtain jet stream is assumed to be uniform at each downstream location and is equal to the average velocity at that location. According to equations (44) and (52)

$$u_{Gx} = 1.52 u_{Gj} \left(\frac{z + x}{w} \right)^{-0.5} \quad (140)$$

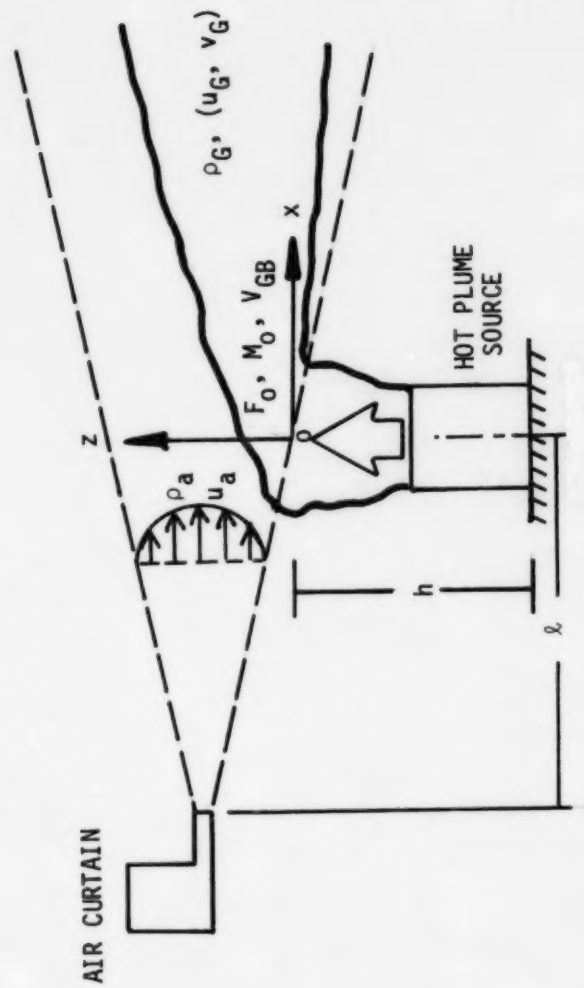


Figure 81. Definition sketch of using an air curtain to contain a hot bouyant plume.

where u_{Gx} = average local velocity of air curtain jet stream, cm/s
 u_{Gj} = air curtain nozzle exit velocity, cm/s
 z = distance from air curtain nozzle to where hot plume and jet stream meet, cm
 w = air curtain slot width, cm
 x = horizontal coordinate, cm

It can be shown by conservation of mass and heat and by the equation of state, the buoyancy flux remains approximately constant along the plume, i.e.,

$$\int_A g(\rho_a - \rho_G) u_{Gx} dA = F_0 \rho_a \quad (141)$$

where F_0 = buoyancy flux at the source, cm^4/s^3
 g = gravitational acceleration, cm/s^2
 A = vertical cross-sectional area of the plume, cm^2
 u_{Gx} = horizontal velocity component of the plume, cm/s
 ρ_a = density of air curtain jet stream, g/cm^3
 ρ_G = density of gas in plume, g/cm^3

The integration is performed over a vertical plume cross-section.

Neglecting the pressure drag, the equation governing the change of vertical momentum flux is:

$$\frac{d}{dx} \int_A \rho_g u_{Gx} v_G dA = \int_A g(\rho_a - \rho_G) dA \quad (142)$$

where v_G = vertical velocity component of plume, cm/s
 x = horizontal coordinates, cm

Since it is assumed that horizontal velocity in the plume is equal to the cross flow, equation (142) is simplified to:

$$\frac{d}{dx} \int_a \rho u_{Gx} v_G dA = \frac{\rho_a F_0}{u_{Gx}} \quad (143)$$

Substituting equation (140) into equation (143) and performing the integration with respect to "x", the following is obtained

$$\int_A \rho_g u_{Gx} v_G dA = \frac{\rho_a F_o}{2.28 u_{Gj} w^{0.5}} \left[(z + x) - z^{1.5} \right] + M_o \quad (144)$$

We will take an approach similar to that given by Turner (1973) and Chu Goldberg (1974), by defining a characteristic radius or the effective plume radius "b" as:

$$\int_A \rho_g u_{Gx} v_G dA = \pi \rho_a b^2 u_{Gx} V \quad (145)$$

in which V = the plume rise velocity given by

$$V = u_{Gx} \frac{dz}{dx} \quad (146)$$

Based on the self-preservation hypothesis, the advancement on the turbulent and laminar interface depends only on local length scale "b" and local velocity scale "V". From dimensional considerations

$$\frac{db}{dx} = \alpha \frac{V}{u_{Gx}} \quad (147)$$

in which α = the entrainment constant introduced by Morton et al. (1956). Taking " α " to be a constant, equations (146) and (147) can be integrated to give a linear growth of the plume with respect to "z", i.e.,

$$b = \alpha z \quad (148)$$

By substituting equations (145), (146), and (148) into equation (144) the following equation is obtained:

$$\pi \rho_a \alpha^2 z^2 u_{Gx} \frac{dz}{dx} = \frac{\rho_a F_0}{2.28 u_G w^{0.5}} [(l+x)^{1.5} - l^{1.5}] + M_0 \quad (149)$$

Upon integration and rearranging, the final form for the plume trajectory is:

$$z = \left(\frac{3}{\alpha^2} \right)^{1/3} \left\{ \frac{F_0 [2(l+x)^{3.5} - 3.5l^{1.5}(l+x)^2 + 1.5l^{3.5}]}{36.9 u_{Gj}^3 w^{1.5} \pi} + \frac{M_0 [(l+x)^2 - l^2]}{4.6 \pi \rho_a u_{Gj}^2 w} \right\}^{1/3} \quad (150)$$

- where
- F_0 = buoyancy flux from source, cm^4/s^3
 - M_0 = vertical momentum flux from source, cm^3/s^3
 - l = distance between source and air curtain, cm
 - u_{Gj} = air curtain nozzle exit velocity, cm
 - w = air curtain slot width, cm
 - x = distance from source, cm
 - z = height of vertical plume rise in air curtain jet stream, cm
 - α = entrainment constant, dimensionless
 - ρ_a = density of air curtain jet stream, g/cm^3

Air Curtain Trajectory

Due to the buoyancy force the hot plume exerts on the air curtain jet stream, the jet stream curves upward. The trajectory of the air curtain jet stream can be predicted by performing a momentum balance on the jet stream. If the diameter of the hot plume is small compared to the distance between the source and the scrubber inlet hood, the trajectory of the jet stream would be equivalent to that of a warm air jet. Abramovich (1963), by balancing the buoyancy force with the vertical acceleration of the mass flow of the jet stream, obtained the following equation for the axis of a curved jet.

$$\frac{dz^*}{dx^*} = \frac{g b_o (T_G - T_a)}{u_{Go}^2 T_a} \int \frac{dx^*}{u_{Gx}} \quad (151)$$

- where
- b_o = jet stream half thickness where the hot plume and the air curtain jet stream meet, cm
 - g = gravitational acceleration, cm^2/s
 - T_G = jet stream gas temperature, $^{\circ}\text{K}$
 - T_a = ambient air temperature, $^{\circ}\text{K}$
 - u_{Go} = average jet stream velocity where the hot plume and jet stream meet, cm/s
 - u_{Gx} = average jet stream velocity, cm/s
 - x^* = x/b_o = dimensionless horizontal coordinate, dimensionless
 - z^* = z/b_o = dimensionless vertical coordinate, dimensionless

Substituting the expression for " u_{Gx} " into equation (151), the equation for the axis of a curved jet is:

$$\frac{z}{b_o} = \frac{0.1 g b_o (T_G - T_a)}{u_{Go}^2 T_a} \left(\frac{x}{b_o} \right)^{2.5} \quad (152)$$

- where
- x = horizontal coordinate, cm
 - z = vertical coordinate, cm

If the plume diameter is about equal to the distance between the source and the scrubber, the jet axis trajectory can be calculated from the equation presented by Hayes (1969) for an air curtain located at the doorway of a heated room. Hayes' equation is an integral equation which requires numerical integration.

Experiments

Figure 82 shows the equipment setup for hot source experiments. The hot source was simulated with an open top furnace which had a natural gas open

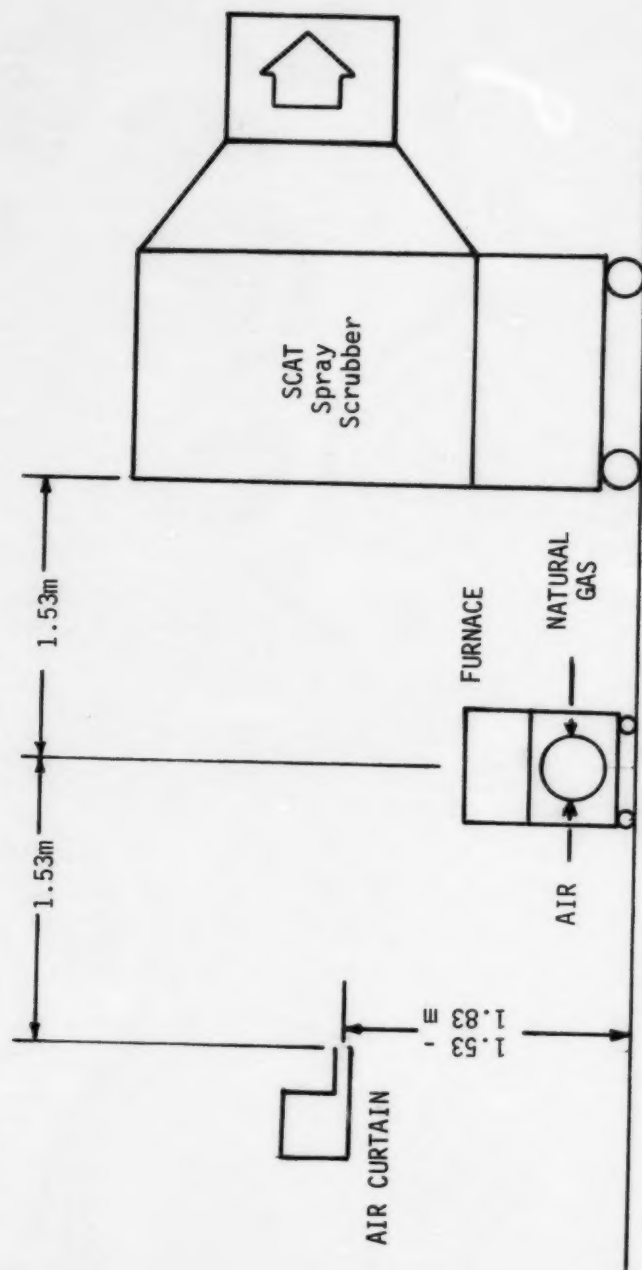


Figure 82. Hot source experiment.

flame burner with maximum capacity of 3.52×10^5 J/s (1,200,000 Btu/hr). The furnace was a rectangular box lined with insulating fire-bricks. There was a 51 cm x 51 cm (20 inches x 20 inches) opening on the top of the furnace for the hot gas to exit.

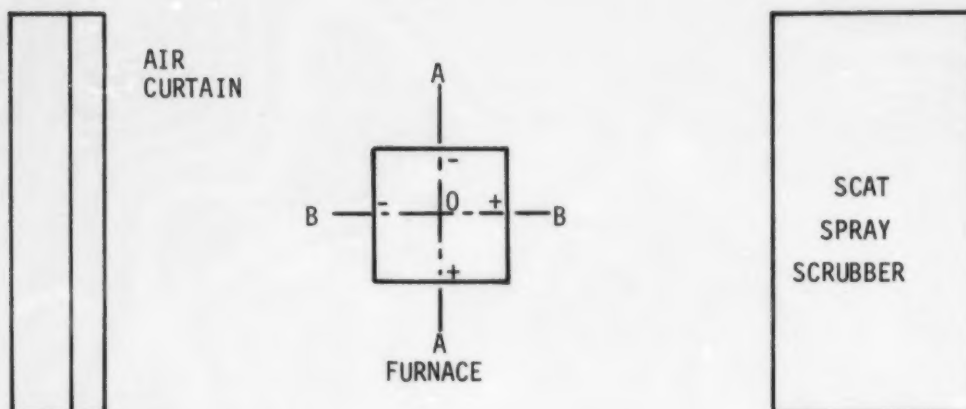
Hot plume containment depends on several parameters, such as buoyancy flux and momentum flux of the plume, momentum of the air curtain jet stream, and the relative position of the plume, air curtain manifold, and scrubber. In hot source experiments, the operation of the burner, the positions of manifold, furnace, and spray scrubber were fixed. The furnace was located at the center of the SCAT system. The distance between the air curtain manifold and the spray scrubber was 3.1 m (10 ft.). The momentum of the air curtain jet stream was changed by using different slot widths and exit velocities.

In starting the experiment, the air curtain was turned off and the hot plume upward velocity and temperature profiles were measured with a mechanical wind velocity meter and a thermocouple, respectively. Next, the burner was turned off and the SCAT system was turned on. The ceiling air curtain velocity and temperature profiles were measured for several locations. The measurements were repeated with the hot source turned back on. The effect of buoyancy on air curtain flow field was deduced by comparing the velocity and temperature profiles before and after the burner was turned on. Flyash particles were also injected into the furnace, with the ceiling air curtain off and on, for hot plume trajectory observations.

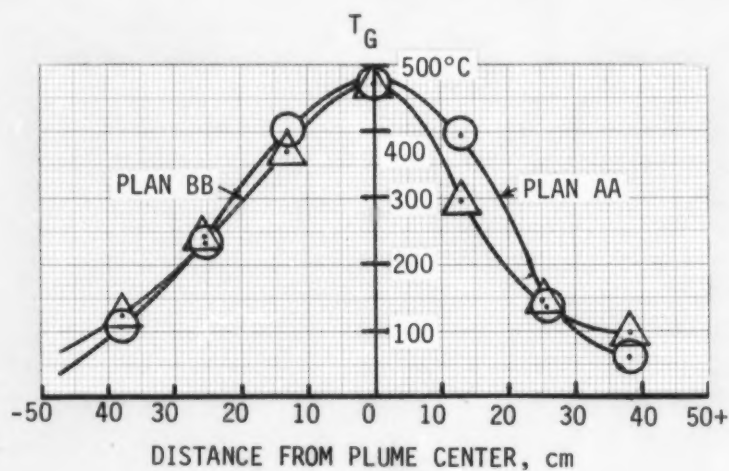
Experimental Data

Experiments were done for three air curtain slot widths (2.5 cm, 5.1 cm, and 10.2 cm), four slot exit velocities (1,017 cm/s, 1,520 cm/s, 2,000 cm/s, and 2,500 cm/s), and two air curtain locations (91 cm and 61 cm above the top of the furnace).

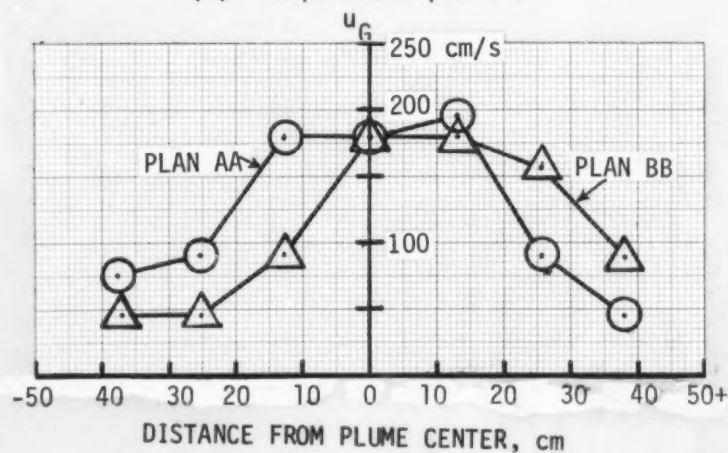
Most of the experiments were performed with the ceiling air curtain exit axis located 61 cm (2 ft) above the top of the furnace. Figure 83 shows the temperature and velocity profiles of the hot plume measured at 61 cm above the



(a) Top view of experimental setup.



(b) Temperature profile



(c) Velocity profile

Figure 83. Temperature and velocity profiles of the hot plume measured at 61 cm above the furnace.

furnace top with no air curtain flow. From Figure 83, the peak velocity and temperature at this location were about 190 cm/s (4.3 mph) and 417°C (880°F), respectively.

Figure 84 shows typical air curtain centerline velocity and temperature profiles before and after the furnace was turned on. Before the furnace was turned on, the centerline velocity was the maximum velocity of the jet stream. After the furnace was turned on, due to the buoyancy of the hot plume, the axis of the air curtain shifted upward. This caused a lower velocity at the same location.

Except for small air curtain slot width (2.5 cm or less) coupled with low exit velocity (20 m/s or less), the air curtain was capable of containing the hot plume.

Plume trajectory was observed visually. Figure 85 shows a sketch of a typical flow pattern. At low air curtain slot exit velocities, the hot plume mixed well with the air curtain flow vertically, i.e. penetration of hot plume into air curtain jet stream was high. There was little mixing in the horizontal direction. As the slot exit velocity was increased, the vertical mixing decreased while horizontal mixing increased. At slot velocities exceeding 25.4 m/s (5,000 fpm), the hot plume did not penetrate through the centerline of air curtain.

The estimated plume dimensions and the center of the plume at the SCAT scrubber inlet surface are presented in Table 15. The buoyancy flux and momentum flux, calculated based on the measured temperatures and velocities at furnace exit, were $1.5 \times 10^8 \text{ cm}^4/\text{s}^3$ and $5.7 \times 10^4 \text{ cm}^3/\text{s}^2$, respectively.

Comparison Between Theory and Data

The plume center at scrubber inlet can be predicted from equation (150) by letting $x = 152 \text{ cm}$. The calculated plume centers are plotted against that measured in Figure 86. It should be noted that the plume rise calculated from equation (150) is with respect to the origin. In deriving equation (150), the

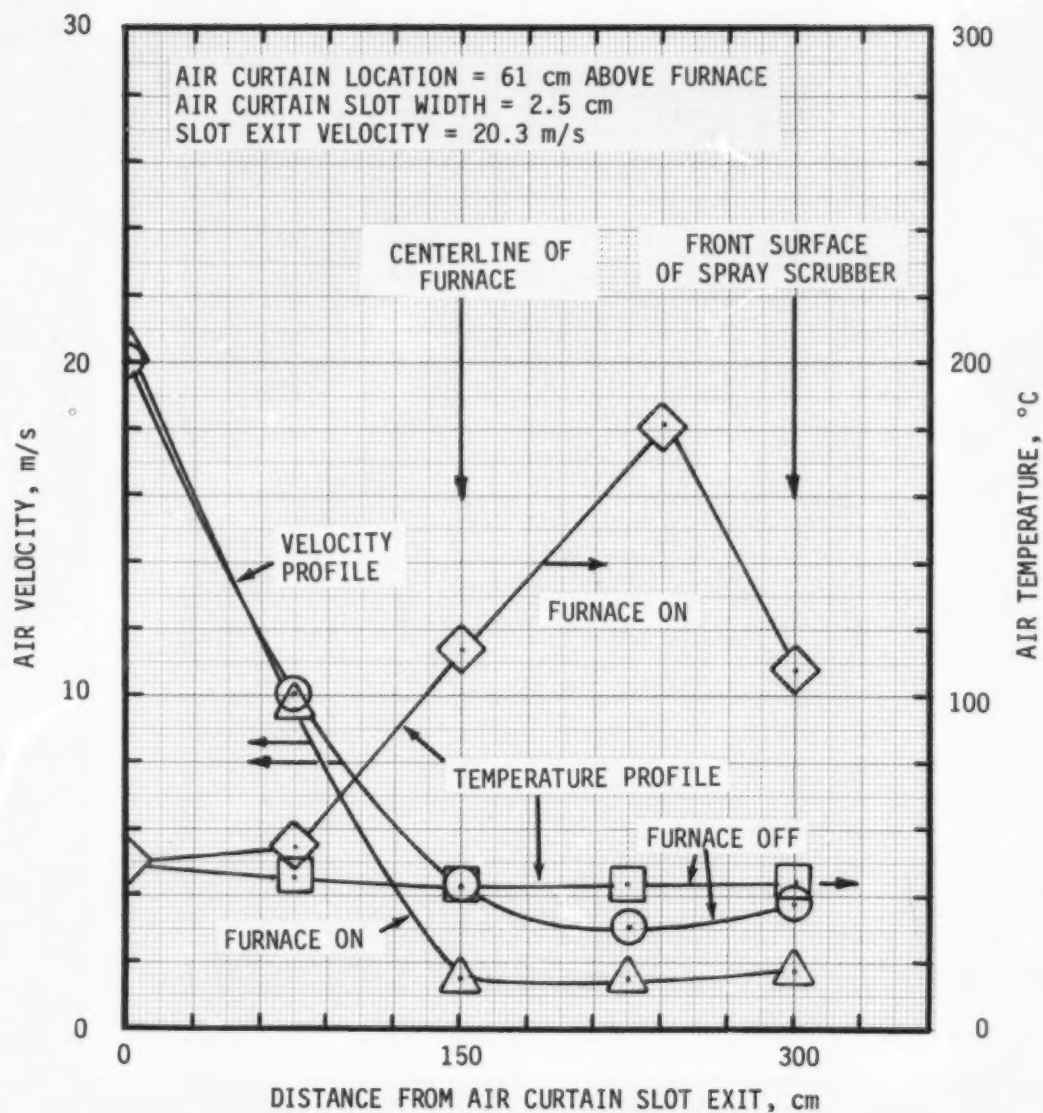


Figure 84. Velocity and temperature profiles at a location 61 cm above the furnace.

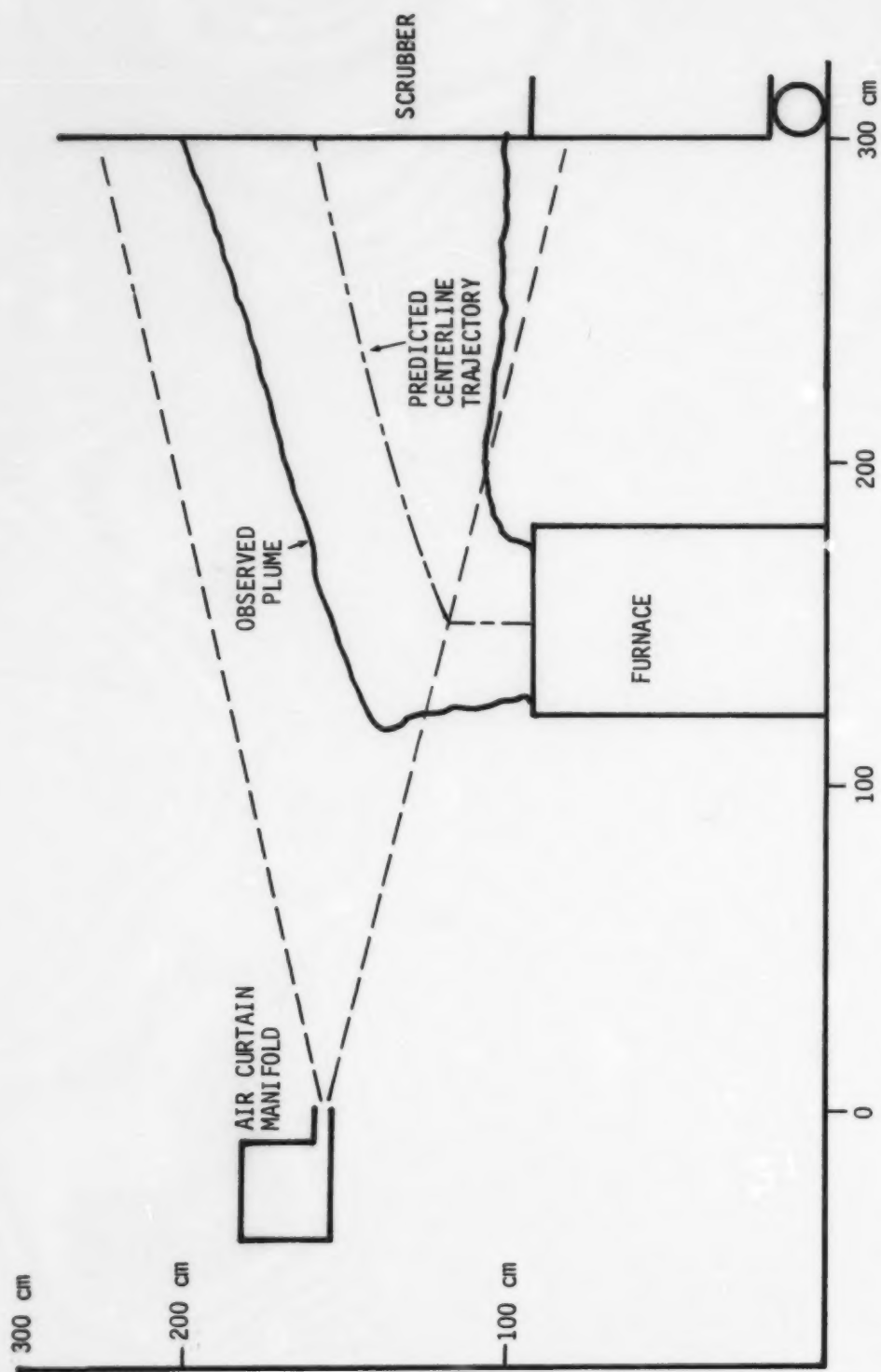


Figure 85. Hot plume trajectory and flow pattern.

TABLE 15. HOT SOURCE EXPERIMENTAL DATA

$$F_o = 1.5 \times 10^8 \text{ cm}^4/\text{s}^3$$

$$M_o = 5.7 \times 10^4 \text{ cm}^3/\text{s}^2$$

Run No.	AIR CURTAIN		Hot plume dimensions at Scrubber Inlet		Hot plume center (height above ground) cm
	Location (height above ground), cm	Slot width, cm	Exit velocity, cm/s	Horizontal width, cm	Vertical width, cm
1-1	153	2.5	1017	92	*
1-2			1525	92	*
1-3			2032	92	102
1-4			2542	92	102
2-1	153	5.1	1017	91	175
2-2			1525	122	127
2-3			2032	122	102
2-4			2542	173	71
3-1	153	10.2	508	127	178
3-2			1017	114	135
3-3			1525	114	76
3-4			2032	92	92
4-1	183	5.1	2542	142	153
4-2			2032	122	170
					153
					189
					165
					153
					137
					*
					169
					140
					140
					165
					177

*Part of the hot plume did not flow into the scrubber.

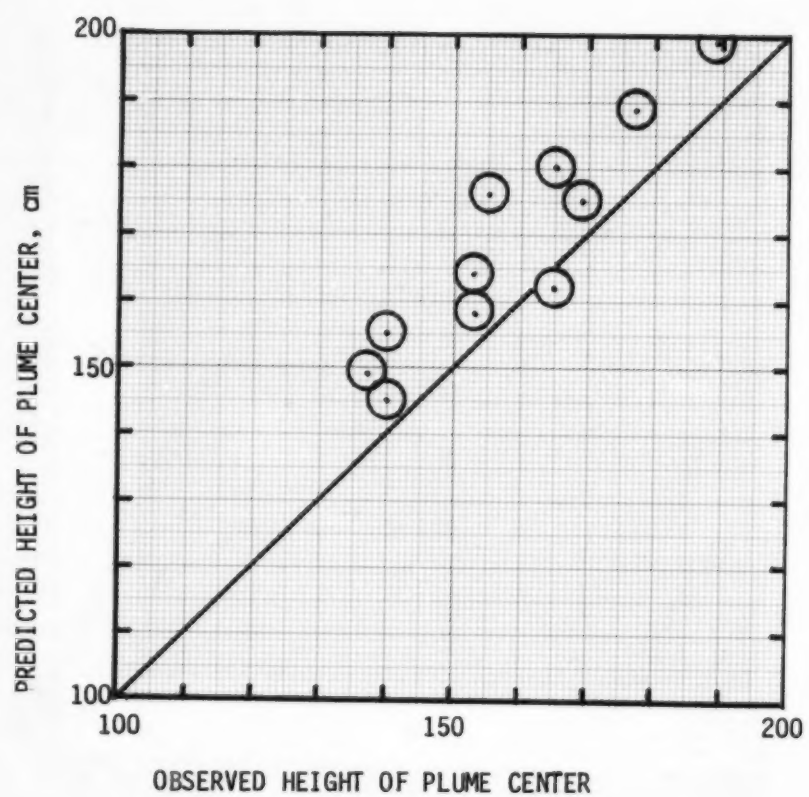


Figure 86. Predicted and observed plume rise.

origin was taken to be where the hot plume first met the air curtain jet stream. The origins, which were estimated from jet expansion angle, were 114 cm and 145 cm above the ground for air curtain jet stream centerlines located at 61 cm and 91 cm above the furnace, respectively. Since the reported plume centers were measured with respect to the ground, the distance between the ground and the origin was added to the calculated plume rise height.

As can be seen from Figure 86, the calculated plume centers were about 10-20 cm higher than that measured. Since the estimations of plume dimensions (by visual observation) and the origin could be off by a few centimeters, the discrepancy between predictions and observation is acceptable.

The measured momentum flux is small compared to buoyancy flux. Therefore, the two-thirds law for pure buoyancy plume rise might be applicable. The plume centers were calculated from equation (136) with $C_5 = 1.6$, as recommended by Briggs (1969). The crosswind velocity was assumed to be a constant and was equal to the average air curtain jet stream velocity at the hot gas exit plane. The predicted plume centers based on Brigg's plume rise formula are almost identical to that calculated from equation (150). Since Brigg's formula is simpler, it could be used for predicting the trajectory of a pure buoyant plume. For forced plumes, i.e. plumes with both buoyancy flux and momentum flux, equation (150) should be used.

SECTION 7

FUTURE RESEARCH RECOMMENDATIONS

The objectives of studying the feasibility of using the SCAT system for controlling industrial process fugitive emissions have been achieved in this study through theoretical and experimental evaluations. It has been shown that the air curtain(s) of the SCAT system can contain, divert, and convey fugitive emissions from buoyant and non-buoyant sources. It can also be used to deflect crosswind.

Most of the basic research for the SCAT system has been done in this study. The theories and experimental data presented in this report will enable one to design a SCAT system. However, there are some additional studies which need to be done to optimize the SCAT design. Future research and development work is needed in the following areas:

1. The effect of obstacles on air curtain flow field. One of many SCAT features which suit it for fugitive emission control is unobstructiveness. Therefore, workmen and equipment, e.g., cranes, can pass freely and work on the source. The presence of workmen and equipment may create turbulence and change the air curtain flow field.
2. The optimal design of receiving hood to intercept the air curtain jet stream.

Since there is no single fixed design for the SCAT system, additional research in the laboratory will not generate much generally useful design information. We recommend a pilot study on an actual fugitive emission source to demonstrate the feasibility of using the SCAT system for controlling the emissions. Since electric arc furnaces, coke ovens, copper converters, etc., are the major fugitive emission sources and the plumes from these sources are hot, an ideal demonstration candidate would be on one of these sources.

To aid the design of the demonstration plant, some small-scale experimental work, such as the effect on obstacles on air curtain flow field, optimal receiving hood, etc., should be conducted concurrently. In outline, the objectives consist of the following tasks:

1. Locate suitable demonstration site
2. Conduct small-scale experiments to obtain design information for the site
3. Design the SCAT system
4. Fabricate, install, and start up the demonstration plant
5. Prepare a detailed test plan describing
 - a. the proposed test matrix
 - b. measurements and measurement techniques to be used
 - c. data handling method
6. Conduct test program
7. Analyze data, conduct engineering and cost analyses, and estimate control effectiveness
8. Based on the above analyses, design and estimate the cost of a SCAT on similar sources

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APPENDIX "A"
CASCADE IMPACTOR DATA

TABLE A1. CASCADE IMPACTOR DATA FOR RUN # 70/8/6

Stage No.	Inlet			Outlet		
	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)
Probe						
1	26	58.5	20.1	10.6	22.9	19.7
2	24	54.0	8.8	10.4	22.5	8.6
3	23.6	53.1	3.4	9.7	20.9	3.3
4	7.2	16.2	1.7	4.7	10.2	1.7
5	1.6	3.6	1.0	1.7	3.7	1.0
6	0.1	0.23	0.59	0.5	1.1	0.57
7	0	0	0.32	0.1	0.22	0.3
Filter	0	0		0	0	

TABLE A2. CASCADE IMPACTOR DATA FOR RUN # 70/8/7

Stage No.	Inlet			Outlet		
	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)
Probe						
1	51.7	116.7	20.2	15.4	33.3	19.7
2	51.5	116.2	8.8	15.2	32.8	8.6
3	35.3	79.7	3.4	14	30.2	3.3
4	11.9	26.9	1.8	6.1	13.2	1.7
5	3.4	7.7	1.0	2.1	4.5	1.0
6	0.7	1.6	0.59	0.7	1.5	0.57
7	0.5	1.1	0.32	0	0	0.3
Filter	0.3	0.68		0	0	

TABLE A3. CASCADE IMPACTOR DATA FOR RUN # 70/8/8

Stage No.	Inlet			Outlet		
	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)
Probe						
1	71.9	256.3	19.9	7.8	28.3	20.0
2	43	153.3	8.7	7.7	27.9	8.8
3	28.1	100.2	3.4	7.6	27.5	3.4
4	12.3	43.8	1.7	3.7	13.4	1.8
5	5.1	18.2	1.0	1.6	5.8	1.0
6	3.1	11.1	0.58	0.7	2.5	0.58
7	2.0	7.1	0.31	0.4	1.5	0.30
Filter	1.4	5		0.3	1	

TABLE A4. CASCADE IMPACTOR DATA FOR RUN # 70/8/9

Stage No.	Inlet			Outlet		
	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)
Probe						
1	36	104.7	19.8	5.8	17.1	20
2	35.9	104.4	8.7	5.5	16.3	8.8
3	19.3	56.1	3.4	5.5	16.3	3.4
4	8.0	23.3	1.7	2.8	8.3	1.7
5	2.8	8.1	1.0	1.00	3.0	1.0
6	1.3	3.8	0.58	0.4	1.2	0.58
7	0.7	2.0		0.1	0.3	
Filter						

TABLE A5. CASCADE IMPACTOR DATA FOR RUN # 70/8/10

Stage No.	Inlet			Outlet		
	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)
Probe						
1	37.6	137.1	19.9	5.4	20	20
2	26.8	97.7	8.7	5.3	19.6	8.8
3	16.2	59.1	3.4	4.6	17.0	3.4
4	6.7	24.4	1.7	2.7	10	1.8
5	2.8	10.2	1.0	0.9	3.3	1
6	1.3	4.7	0.58	0.2	0.74	0.58
7	1.0	3.7	0.31	0	0	0.3
Filter	0.8	2.9		0	0	

TABLE A6. CASCADE IMPACTOR DATA FOR RUN # 70/8/11

Stage No.	Inlet			Outlet		
	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)
Probe						
1	24.9	72.9	19.9	8.4	24.9	20
2	20.0	58.6	8.7	8.1	24	8.8
3	15.4	45.1	3.4	7.6	22.5	3.4
4	6.8	19.9	1.7	5.5	16.3	1.8
5	3.1	9.1	1.0	3.2	9.5	1.0
6	1.3	3.8	0.58	2.0	5.9	0.58
7	1.0	2.9	0.31	1.2	3.6	0.3
Filter	0.9	2.6		0.8	2.4	

TABLE A7. CASCADE IMPACTOR DATA FOR RUN # 70/8/14

Stage No.	Inlet			Outlet		
	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)
Probe						
1	41.3	100.4	20.3	4.6	10.6	19.8
2	23.5	57.2	8.9	4.6	10.6	8.7
3	10.1	24.6	3.4	4.3	9.9	3.6
4	3.8	9.2	1.8	2.8	6.5	1.7
5	1.8	4.4	1.0	1.5	3.5	1.0
6	0.6	1.5	0.59	0.6	1.4	0.57
7	0.1	0.24	0.32	0.2	0.46	0.3
Filter	0.1	0.24		0.1	0.23	

TABLE A8. CASCADE IMPACTOR DATA FOR RUN # 70/8/15

Stage No.	Inlet			Outlet		
	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)
Probe						
1	6.0	21.2	27.1	1.8	9.4	26.4
2	5.9	20.9	11.9	2.7	9.1	11.6
3	5.6	19.8	4.6	2.6	8.8	4.5
4	3.2	11.3	2.4	2.1	7.1	2.3
5	1.5	5.3	1.4	1.2	4.0	1.3
6	0.6	2.1	0.79	0.6	2.0	0.77
7	0.3	1.1	0.43	0.4	1.4	0.4
Filter	0.2	0.71		0.3	1.0	

TABLE A9. CASCADE IMPACTOR DATA FOR RUN # 70/8/18

Stage No.	Inlet			Outlet		
	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)
Probe						
1	11.6	21	10.2	5.6	9.8	19.8
2	11.4	20.6	8.8	5.6	9.8	8.7
3	10.7	19.4	3.4	5.6	9.8	3.4
4	5.6	10.1	1.8	3.8	6.6	1.7
5	1.7	3.1	1.0	1.1	1.9	1
6	0.3	0.54	0.59	0.1	0.17	0.58
7	0	0	0.32	0	0	0.3
Filter	0	0		0	0	

TABLE A10. CASCADE IMPACTOR DATA FOR RUN # 70/8/19

Stage No.	Inlet			Outlet		
	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)
Probe						
1	9.2	16.6	20.2	4.9	8.6	19.8
2	9.0	16.3	8.8	4.9	8.6	8.7
3	8.3	15.0	3.4	4.9	8.6	3.4
4	4	7.2	1.8	3.1	5.4	1.7
5	1.2	2.2	1.0	1.0	1.8	1.0
6	0.2	0.36	0.59	0.1	0.17	0.58
7	0	0	0.32	0	0	0.3
Filter	0	0		0	0	

TABLE A11. CASCADE IMPACTOR DATA FOR RUN # 70/9/1

Stage No.	Inlet			Outlet		
	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)
Probe						
1	26.5	58.56	25.8	3.06	7.16	26.5
2	22.84	50.47	11.3	3.06	7.16	11.6
3	19.62	43.35	4	3.06	7.16	4.5
4	7.11	15.71	6.21	1.63	3.81	2.2
5	0.88	1.94	1.2	0.49	1.15	1.3
6	0.13	0.29	0.75	0.04	0.09	0.71
7	0.09	0.20	0.37	0	0	0.42
Filter	0.06	0.13		0	0	

TABLE A12. CASCADE IMPACTOR DATA FOR RUN # 70/9/2

Stage No.	Inlet			Outlet		
	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)
Probe						
1	6.18	14.66	25.3	2.66	6.92	26.5
2	4.53	10.75	11.1	2.28	5.93	11.6
3	4.28	10.15	4.3	2.28	5.93	4.5
4	3.56	8.45	2.0	2.17	5.65	2.2
5	2.37	5.62	1.2	1.59	4.14	1.3
6	1.15	2.73	0.74	0.85	2.21	0.71
7	0.19	0.45	0.36	0.08	0.21	0.42
Filter	0	0		0	0	

TABLE A13. CASCADE IMPACTOR DATA FOR RUN # 70/20/1

Stage No.	Inlet			Outlet		
	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)
Probe						
1	97.6	151.6	25.2	17.5	27.2	25.2
2	69.5	108	11.0	17.4	27.0	11.0
3	43.2	67.1	4.3	17.3	26.9	4.3
4	19.1	27.7	2.2	14.6	22.7	2.2
5	11.5	17.9	1.4	9.7	15.1	1.3
6	7.0	10.9	0.72	5.7	8.9	0.72
7	2.9	4.5	0.37	2.4	3.7	0.4
Filter	0.9	1.4		0.6	0.93	

TABLE A14. CASCADE IMPACTOR DATA FOR RUN # 70/20/5

Stage No.	Inlet			Outlet		
	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)
Probe						
1	9.4	25.4	24.7	3.3	10.1	26.3
2	9.0	24.3	10.8	3.3	10.1	11.5
3	8.8	23.8	4.2	3.3	10.1	4.5
4	7.3	19.7	2.2	3.0	9.2	2.3
5	4.3	11.6	1.3	2.2	6.7	1.3
6	1.6	4.3	0.71	1.2	3.7	0.76
7	0.5	1.4	0.36	0.3	0.92	0.42
Filter	0	0		0.2	0.61	

TABLE A15. CASCADE IMPACTOR DATA FOR RUN # 70/20/8

Stage No.	Inlet			Outlet		
	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)
Probe						
1	15.5	22.6	24.2	4.0	6.6	25.7
2	15	21.9	10.6	3.7	6.1	11.3
3	14.3	20.9	4.1	3.7	6.1	4.4
4	11.9	17.4	2.1	3.5	5.8	23
5	6.4	9.4	1.3	2.6	4.3	1.3
6	2.4	3.5	0.69	0.6	1.0	0.74
7	0	0	0.36	0	0	0.41
Filter	0	0	0	0	0	

TABLE A16. CASCADE IMPACTOR DATA FOR RUN # 70/20/7

Stage No.	Inlet			Outlet		
	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)
Probe						
1	88.6	124.3	23.9	9.3	15.3	25.9
2	66.5	93.3	10.5	8.8	14.5	11.3
3	29.2	41	4.1	8.8	14.5	4.4
4	11.3	15.9	2.1	6.7	11.0	2.3
5	5.6	7.9	1.3	3.9	6.4	1.3
6	2.1	3.0	0.69	1.4	2.3	0.74
7	0.2	0.28	0.35	0.2	0.33	0.42
Filter	0	0		0	0	

TABLE A17. CASCADE IMPACTOR DATA FOR RUN # 70/20/9

Stage No.	Inlet			Outlet		
	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μ m)	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μ m)
Probe						
1	22.4	32.1	23.9	9.0	14.5	25.4
2	21.0	30.1	10.5	8.7	14.0	11.1
3	19.6	28.1	4.1	8.3	13.4	4.3
4	14.0	20.1	2.1	7.4	11.9	2.2
5	8.5	12.2	1.3	4.3	6.9	1.3
6	4	5.7	0.69	2.0	3.2	0.73
7	1.2	1.7	0.35	0.8	1.3	0.41
Filter	0.2	0.29		0	0	

TABLE A18. CASCADE IMPACTOR DATA FOR RUN # 70/20/10

Stage No.	Inlet			Outlet		
	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μ m)	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μ m)
Probe						
1	36.3	53.2	24.2	6.9	11.4	25.7
2	28.7	42.1	10.6	6.7	11.1	11.3
3	23.5	34.4	4.1	6.7	11.1	4.4
4	14.1	20.7	2.1	6	10	2.3
5	8.7	12.8	1.3	3.7	6.1	1.3
6	4	5.9	0.69	1.9	3.2	0.74
7	1.3	1.9	0.36	0.4	0.66	0.41
Filter	0.2	0.29		0	0	

TABLE A19. CASCADE IMPACTOR DATA FOR RUN # 72/10/1

Stage No.	Inlet			Outlet		
	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)
Probe						
1	19.3	28.1	19.4	16.0	16.2	20.4
2	19.0	27.7	8.5	9.7	15.7	9.0
3	18.4	26.8	3.3	9.5	15.3	3.5
4	14.2	20.7	1.7	8.3	13.4	1.7
5	8.1	11.8	0.98	4.9	7.9	1.0
6	3.6	5.2	0.57	2.1	3.4	0.55
7	1.3	1.9	0.30	0.6	1.0	0.33
Filter	0.6	0.9		0.2	0.3	

TABLE A20. CASCADE IMPACTOR DATA FOR RUN # 72/10/4

Stage No.	Inlet			Outlet		
	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)
Probe						
1	122.0	134.0	19.5	22.0	25.9	20.2
2	73.6	80.8	8.5	22.0	25.9	8.8
3	46.0	50.5	3.3	21.4	25.2	3.4
4	19.1	21.0	1.7	13.8	16.3	1.7
5	9.8	10.8	0.98	7.4	8.7	1.0
6	3.5	3.8	0.57	3.0	3.5	0.5
7	0.5	0.6	0.3	0.4	0.5	0.3
Filter	0	0		0.1	0.1	

TABLE A21. CASCADE IMPACTOR DATA FOR RUN # 72/10/16

Stage No.	Inlet			Outlet		
	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)
Probe						
1	271.7	341.7	19.5	34.0	47.1	20.4
2	179.0	225.1	8.5	32.1	44.4	9.0
3	97.7	122.9	3.3	30.4	42.1	3.5
4	32.9	41.4	1.7	17.9	24.8	1.7
5	17.3	21.8	0.98	11.1	15.4	1.0
6	8.7	10.9	0.56	6.1	8.4	0.54
7	3.2	4.0	0.28	2.1	2.9	0.31
Filter	1.0	1.3		0.7	1.0	

TABLE 22. CASCADE IMPACTOR DATA FOR RUN # 72/10/18

Stage No.	Inlet			Outlet		
	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)
Probe						
1	182.1	335.5	19.9	24.0	46.8	20.5
2	147.9	272.5	8.7	22.8	44.4	9.0
3	73.1	134.7	3.4	20.9	40.7	3.5
4	28.0	51.6	1.6	13.2	25.7	1.8
5	13.1	24.1	0.92	8.3	16.2	1.0
6	7.7	14.2	0.58	5.0	9.7	0.59
7	3.2	5.9	0.27	2.4	4.7	0.30
Filter	1.4	2.6		1.3	2.5	

TABLE A23. CASCADE IMPACTOR DATA FOR RUN # 72/10/19

Stage No.	Inlet			Outlet		
	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)
Probe						
1	97.5	212.3	19.5	11.1	26.8	20.5
2	84.0	182.9	8.5	10.3	24.9	9.0
3	38.8	84.5	3.3	9.2	22.3	3.5
4	20.4	44.3	1.6	7.7	18.6	1.8
5	10.9	23.7	0.90	6.4	15.5	1.0
6	5.6	12.2	0.56	4.1	9.9	0.59
7	2.3	5.0	0.27	2.7	6.5	0.31
Filter	0.8	1.7		1.4	3.4	

TABLE A24. CASCADE IMPACTOR DATA FOR RUN # 72/10/20

Stage No.	Inlet			Outlet		
	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)
Probe						
1	161.9	353.5	19.5	9.7	23.5	20.6
2	85.1	185.8	8.6	9.7	23.5	9.0
3	72.9	159.2	3.3	8.9	21.6	3.5
4	19.5	42.6	1.7	7.3	17.7	1.7
5	10.9	23.8	0.98	5.1	12.4	1.0
6	4.8	10.5	0.56	2.9	7.0	0.54
7	1.1	2.4	0.28	0.7	1.7	0.31
Filter	0	0		0.1	0.24	

TABLE 25. CASCADE IMPACTOR DATA FOR RUN # 72/10/21

Stage No.	Inlet			Outlet		
	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)
Probe						
1	119.9	363.1	19.9	29.5	20.3	
2	76.4	231.4	8.7	8.2	26.0	8.9
3	39.9	120.8	3.4	7.7	24.4	3.5
4	15.9	48.2	1.6	6.1	19.3	1.8
5	7.7	23.3	0.92	4.5	14.3	1.0
6	4.3	13.0	0.58	2.7	8.6	0.59
7	1.6	4.9	0.27	1.0	3.2	0.30
Filter	0.5	1.5		0.5	1.6	

TABLE A26. CASCADE IMPACTOR DATA FOR RUN # 72/10/22

Stage No.	Inlet			Outlet		
	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)
Probe						
1	17.4	31.5	19.9	7.5	14.2	20.3
2	16.3	29.5	8.7	7.0	13.2	8.9
3	15.4	27.9	3.4	6.6	12.5	3.4
4	13.6	24.6	1.6	5.9	11.1	1.6
5	8.8	15.9	0.92	4.5	8.5	0.94
6	3.8	6.9	0.57	2.1	4.0	0.59
7	1.1	2.0	0.27	0.4	0.8	0.28
Filter	0.3	0.5		0.1	0.2	

TABLE A27. CASCADE IMPACTOR DATA FOR RUN # 72/10/23

Stage No.	Inlet			Outlet		
	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)
Probe						
1	25.3	45.7	19.8	5.9	11.1	20.3
2	20.8	37.6	8.7	5.8	10.9	8.9
3	16.8	30.3	3.4	5.7	10.8	3.4
4	12.0	21.7	1.6	5.0	9.4	1.8
5	6.9	12.5	0.92	3.7	7.0	1.0
6	3.5	6.3	0.57	1.9	3.6	0.59
7	1.3	2.4	0.27	0.4	0.8	0.30
Filter	0.4	0.7		0.1	0.2	

TABLE A28. CASCADE IMPACTOR DATA FOR RUN # 72/10/24

Stage No.	Inlet			Outlet		
	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)
Probe						
1	44.3	33.5	19.9	15.0	11.8	22.0
2	39.6	29.9	8.7	14.6	11.5	9.6
3	37.1	28.0	3.4	14.4	11.3	3.7
4	32.1	24.3	1.6	12.9	10.2	1.9
5	18.7	14.1	0.92	8.5	6.7	1.1
6	9.0	6.8	0.57	3.4	2.7	0.64
7	2.6	2.0	0.27	0.7	0.6	0.33
Filter	0.1	0.1		0	0	

TABLE A29. CASCADE IMPACTOR DATA FOR RUN # 72/10/25

Stage No.	Inlet			Outlet		
	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)	Cum. Mass (mg)	Cum. Conc. (mg/DNm ³)	Part. Dia. (μm)
Probe						
1	17.7	18.4	19.9	7.8	8.5	20.2
2	17.1	17.8	8.7	7.4	8.1	8.9
3	16.3	17.0	3.4	7.4	8.1	3.4
4	14.8	15.4	1.6	7.0	7.6	1.8
5	8.4	8.7	0.92	5.0	5.5	1.0
6	3.0	3.1	00.57	2.2	2.4	0.58
7	0.6	0.63	0.27	0.3	0.3	0.3
Filter	0	0		0	0	